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The Diffusion of Carbon and the Carburization Process

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SYNOPSIS

The carburization of steel is considered from the point of view of the diffusion of carbon. Mathematical solutions of Fick's law for different conditions are used to calculate types of carbon penetration curves. The difficulty of assessing case depths when the shapes of the curves are different is illustrated. A method is given for calculating the relative active carburizing and soaking times when it is desired to reduce the surface carbon concentration of a carburized case. A series of curves, which have been found a very accurate guide in experimental carburizations, summarize these values.

ALTHOUGH it has been believed for some time that the migration of carbon into steel during carburization is a normal solid diffusion process, early attempts by Runge,[1] Tammann and Schönert,[2] and Bramley and co-workers,[3] to interpret carburization results by means of Fick's law of diffusion were not very successful. This was chiefly because of the difficulty of standardizing experimental conditions so that one of the mathematical solutions of Fick's law could be applied. Bramley and co-workers, in particular, carried out much careful and useful experimental work but, as pointed out by Wells and Mehl,[4] the calculations of the diffusivity coefficient were erroneous because the experimental conditions did not accord with the particular solutions of Fick's law used. In consequence, all values of the diffusivity coefficient given by Bramley are low. More recent experiments by Harris[5] and by Laidler and Taylor[6] have been satisfactorily interpreted by diffusion equations, and values of the diffusivity coefficient which are in substantial agreement with the best values of this constant have been obtained; these are the values of Wells and Mehl[4] and of Paschke and Hauttman,[7] obtained from a study of the diffusion of carbon from high-carbon steel rods welded on to low-carbon steel rods.

From the practical standpoint, the exact magnitude of the diffusivity coefficient is of less interest than is its variation with steel composition, etc. Contrary to the views of earlier workers, it is probable that the diffusivity coefficient is independent of grain size and of the presence of alloying metals, at least within the range of mild steel and low-alloy steel. According to Smoluchowski,[8] however, additions of 2 and 4% of cobalt increase the diffusivity coefficient of carbon in steel, particularly at lower carbon concentrations.

The influence of 'impurities' like oxygen, sulphur, phosphorus, and nitrogen has been studied in detail only by Bramley and co-workers,[9] who reported that nitrogen had no effect, that oxygen and phosphorus caused a slight reduction, and that sulphur caused a pronounced reduction, in the diffusivity coefficient. Unfortunately, in their experiments, no distinction was drawn between the effect on the diffusivity coefficient and the effect on the surface reactivity of the steel, which is known to be reduced by sulphur. In all cases where a reduction in the diffusivity constant was reported, the carbon penetration curves showed that the carbon concentration at the surface was also reduced. The evidence is therefore inconclusive, and as large amounts of 'impurity' were involved in these experiments it is probable that the amounts present in commercial steels will have little or no effect—a conclusion reached also by Wells and Mehl from their experiments. Apart from temperature, which has a pronounced effect, carbon concentration also affects the value of the diffusivity coefficient; this was pointed out by Wells and Mehl, who showed that the diffusivity coefficient increases with increasing carbon concentration.

THE CARBURIZATION OF STEEL AS A DIFFUSION PROCESS

Effect of Carburization Time

It is difficult to apply mathematical analysis to technical carburization processes because of the complexity of the conditions prevailing. What can be done, however, is to consider cases in which the conditions are relatively simple, and to which diffusion equations can be applied, and then to consider more complex systems in the light of the results obtained.

The solution of Fick's law that is most widely

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applicable to carburization problems is as follows :

$$1 - \frac{C}{C_o} = \frac{2}{\sqrt{\pi}} \int_0^{\frac{x}{2\sqrt{DT}}} e^{-y^2} dy \quad \ldots\ldots\ldots\ldots\ldots(1)$$

where C is the carbon potential, *i.e.*, the increase in carbon concentration at a distance x from the surface, C_o is the carbon potential at the surface, D is the diffusivity coefficient, t is the carburizing time, and $e^{-y^2}dy$ is the Gauss error function. Corresponding values of $1 - C/C_o$ and $x/2\sqrt{DT}$ can be obtained from mathematical tables such as, for instance, those given by Burgess.[10] The boundary conditions for which equation (1) is applicable are that C_o and D must be constant ; neither of these conditions can be strictly fulfilled in practice. As already mentioned, D is a function of the carbon concentration, and must therefore vary with the carbon gradient through the carburized case. It will be shown later that the effect of this is not great, and a useful approximation to actual conditions is obtained if D is assumed constant at constant temperature.

A constant surface carbon potential is also impossible to attain in practice ; there must always be a period at the beginning of the carburization during which the value of C_o is increasing. With suitable conditions and except for short carburizing times, this period will be small in comparison with the total time and may be neglected.

For carburizations to which equation (1) applies, the penetration x, to any particular value of C, is a function of the surface carbon potential and is proportional to the square roots of the time and the diffusivity coefficient. In other words, the case depth, to the same value of C, is proportional to the square root of time, when the surface carbon potential and diffusivity coefficient are constant. For carburizations at constant temperature, and for the most common range of carburizing steels of carbon content 0·15–0·20%, these conditions are usually fulfilled and the ' square root rule ' holds good. This is the most widely known and used result of the application of the diffusion law to carburization, but, of course, it is valid only within the stated limits ; this qualification is often overlooked.

A similar relationship holds good for the amount of carbon absorbed during carburization. The amount of carbon absorbed S through an area q of the surface is given by the following equation derived from equation (1) :

$$S = 2C_o q \sqrt{Dt/\pi} \quad \ldots\ldots\ldots\ldots\ldots(2)$$

It follows from this that for unit surface area and constant values of C and D, S is proportional to $\sqrt{t}$. Hence the average rate of absorption $\bar{R}$ of carbon during carburization, which equals S/t, will be proportional to $1/\sqrt{t}$ and the instantaneous rate of absorption $R = dS/dt$ will be proportional to $1/2\sqrt{t}$.

It has been shown[6] that, for gas carburizations up to the maximum rate, the rate of absorption of carbon is a linear function of the gas velocity, $R = kV + A$, where V is the gas velocity required to maintain the maximum rate and A is a constant. Since R varies as $1/2\sqrt{t}$ and $\bar{R}$ varies as $1/\sqrt{t}$, then V varies as $1/2\sqrt{t} - A'$ and $\bar{V}$ varies as $1/\sqrt{t} - A''$, where A' and A'' are constants. The significance of this is apparent from Fig. 1, which shows R and $\bar{R}$ plotted against t in arbitrary units (full curve). If the constants in the above expressions are neglected, the curve can also be taken to represent approximately the manner in which V and $\bar{V}$ should vary with t. The minimum gas flow required to give maximum carburization varies with the progress of the carburization, and therefore the linear gas velocity V would have to be varied continuously over the whole carburizing time, as shown in Fig. 1 (full curve), if it was desired to keep strictly to the minimum ; the velocity at zero time would have to be infinite. This is impossible, and in practice it is sufficient to use average gas velocities over short periods. If the gas flow, and hence the rate of carbon absorption, is at any time less than the theoretical minimum, as it must be in the first half of any averaged period, there will be a drop in the rate of carbon absorption which can never be quite made up. However, experiment shows that when the chosen period is relatively short or the range of the gas velocities is small, this factor has little effect and may be ignored.

The practical value of this may be illustrated by two examples. Suppose that for a carburizing time of 3 hr. a minimum average gas flow of 100 cu. ft./hr. is necessary to give maximum carburization. The same flow, with other conditions remaining constant, for a carburizing time of 20 hr. would require 2000 cu. ft. of gas. Neglecting the constant in the expression $V \propto 1/\sqrt{t} - A''$, it is found from Fig. 1 that the gas flow for a 20-hr. carburization need be only 0·4 of the flow for a 3-hr. carburization, *i.e.*, 800 cu. ft. of gas during the whole 20 hr. This is too long a period over which to average the flow, but by dividing the carburizing time as follows, a figure near 800 is obtained :

Time, hr.	Average Gas Flow, cu. ft./hr.	Total Gas, cu. ft.
0–3	100	300
3–10	40	280
10–20	24	240
		820

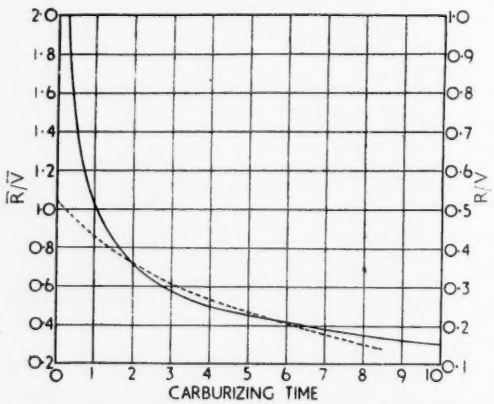


Fig. 1—Variation of maximum instantaneous and average rates of carbon absorption (and required gas velocity) with carburizing time (full curve). Probable variation in pack carburization (broken curve). Arbitrary units

Shorter periods would give a closer approach to ideal flow conditions, but would not be worth while.

Consider now a shorter carburizing time. In the foregoing example the wasting of gas was involved, but now the desired result will not be obtained unless it is realized that a gas flow sufficient for a 3-hr. carburization is inadequate for a shorter period. Suppose the carburizing time to be reduced to 1 hr. Reference to Fig 1 shows that, on the basis of 100 cu. ft./hr. for a 3-hr. carburization, 180 cu. ft./hr. is the minimum average flow for a 1-hr. carburization.

Although only approximations, these calculations indicate the magnitude of the gas-flow variation for efficient working. This variation may be important, and Fig. 1 provides a useful rough guide in the choice of correct rates of flow.

The extremely rapid rate of carbon absorption in the initial stages gives gas carburization a great advantage over pack carburization for thin-case work. Even the most highly ' energized ' compounds cannot supply carbon to the surface of the steel rapidly enough to take advantage of the high rate of absorption, which is limited by the rate of supply of atomic carbon and not by the rate of carbon diffusion. In gas carburization, fuller advantage can be taken of the high initial absorption rates.

Lowering the Surface Carbon Concentration by Soaking

Only gas carburizations in which the carbon concentration at the steel surface is quickly raised to a definite value and thereafter maintained constant, have so far been considered. In practice, this usually means a surface carbon concentration at or near the solubility limit of carbon in austenite. At present the methods available for lowering the surface carbon concentration are : (1) Decreasing the temperature ; (2) manipulating the gas composition and rates of flow ; and (3) following the active carburizing by a soaking period in a neutral atmosphere.

The effect of decreasing the temperature is limited and has the disadvantage that carburization is slowed down. Manipulating the gas composition and rates of flow is also far from satisfactory. It is not easily possible to achieve even flow throughout a retort, and uneven flow will lead to uneven carburization unless it is great enough to ensure maximum carburization throughout. The most satisfactory technical method is undoubtedly to carburize at the maximum rate and then to reduce the surface carbon concentration to the desired value by soaking in a neutral atmosphere, obtained by cutting off the flow of gas. The problem of determining the active carburizing and soaking times therefore arises. Harris[5] gave a solution based on equation (1). During the soaking period, however, the surface carbon concentration decreases continuously, and therefore equation (1) is not suitable. A rigorous mathematical treatment of the problem would be exceedingly difficult and would lead to an expression too complicated for practical purposes. An approximate solution can be derived as follows : Bramley and co-workers used the following equation derived by Carslaw[11] for the diffusion of heat :

$$C = \frac{H}{2\sqrt{\pi DT}} \cdot e^{-\frac{x^2}{4DT}} \quad \ldots\ldots\ldots\ldots\ldots\ldots(3)$$

where C is the carbon potential at a distance x from the surface, D is the diffusivity coefficient, t is the diffusion time, and H is a constant representing twice the amount of carbon diffusing under unit area of surface.

As derived by Carslaw, this represents the diffusion of an instantaneous plane source of heat H in an infinite solid. Applied to carburization in a semi-infinite solid it represents the diffusion of a quantity $H/2$ of carbon released in unit area of surface at zero time. Bramley and co-workers found that many of their carburization results gave curves corresponding very closely to those given by equation (3). Nevertheless, its application to straight carburization results is inadmissible because the boundary conditions of the equation are not fulfilled.

In the soaking period, after active carburization is stopped, diffusion will tend to approximate more and more to that represented by equation (3). There is now a constant amount of carbon diffusing and, as Bramley's results show, the distribution of carbon at the end of the active carburizing period approximates fairly closely to the distribution represented by equation (3). As a first approximation, it may be assumed that the change-over from one type of diffusion to the other occurs instantaneously at the end of the active carburizing period. If the amount of carbon is known, the total time required to reach a given surface carbon concentration, for the conditions represented by equation (3), may be calculated. The difference between this time and the active carburization time does not, however, represent the soaking time. Diffusion must be slower when carbon is being continuously absorbed than when the total amount is instantaneously absorbed at zero time. But if the diffusion time for the latter conditions, which gives a distribution curve approximating most closely to that at the end of the active carburization time, can be found, then the difference between these two hypothetical times will represent the soaking time. This has been obtained as follows. The carbon distribution curve at the end of an active carburizing time t_1 is represented by the equation :

$$1 - \frac{C}{C_0} = \frac{2}{\sqrt{\pi}} \int_0^{\frac{x}{2\sqrt{Dt_1}}} e^{-y^2} dy \quad \ldots\ldots\ldots\ldots(1a)$$

It is assumed that this changes instantaneously to a carbon distribution curve of the type represented by equation (3). If t_2 is the hypothetical time for which, under the latter conditions, the carbon distribution curve closely approximates to that given by equation (1a), then :

$$C = \frac{H}{2\sqrt{\pi Dt_2}} \cdot e^{-\frac{x^2}{4Dt_2}} \quad \ldots\ldots\ldots\ldots\ldots\ldots(3a)$$

But the amount of carbon absorbed per unit area at the end of the active carburizing period is :

$$S = 2C_0\sqrt{\frac{Dt_1}{\pi}} \quad \ldots\ldots\ldots\ldots\ldots\ldots(2a)$$

$$\therefore \quad H = 4C_0\sqrt{\frac{Dt_1}{\pi}} \quad \ldots\ldots\ldots\ldots\ldots\ldots(4)$$

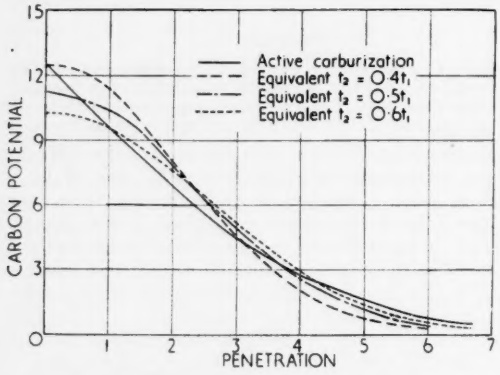


Fig. 2—Calculated carbon penetration curves for constant carbon potential and constant amount of carbon diffusing. Arbitrary units

Substituting in equation (3*a*):

$$C = \frac{2C_o}{\pi}\sqrt{\frac{t_1}{t_2}} \cdot e^{-\frac{x^2}{4Dt_2}} \quad \text{.....(5)}$$

In order to assess the value of t_2 in terms of t_1, a definite value is assigned to t_1, say, 100 hr., and the carbon distribution curve is worked out from equation (1*a*). Similarly, a series of distribution curves is calculated from equation (5) by assigning definite values to t_2. The value of t_2 is then taken as that value which gives a distribution curve most closely approximating to the curve calculated from equation (1*a*). Numerical values must also be assigned to C and D for the purpose of calculation, but it was found that varying C between 1·0 and 1·52%, and D between $10{\cdot}0 \times 10^{-8}$ and $45{\cdot}5 \times 10^{-8}$, had no material effect on the matching of the curves. A series of such curves is shown in Fig. 2, and the best value of t_2 is assessed at about $0{\cdot}5$–$0{\cdot}6\,t_1$. For reasons discussed later, when considering the effect of the variation of diffusivity coefficient with carbon concentration, t_2 has been taken as $0{\cdot}5\,t_1$.

Similarly, the hypothetical time required to give a desired surface carbon potential according to equation (3) may be expressed in terms of t_1. If t_3 represents this time and c_o is the desired potential, *i.e.*, $x = 0$, then substituting in equation (5)

$$c_o = \frac{2C_o}{\pi}\sqrt{t_1/t_3}$$

or

$$t_3 = \frac{4}{\pi^2} \cdot \frac{C_o^2}{c_o^2} \cdot t_1 \quad \text{.....(6)}$$

From this expression t_3 can be calculated in terms of t_1 for given values of C_o and c_o. The difference, $t_3 - t_2$, between these two hypothetical times gives the actual soaking time required, and the total carburizing time T is equal to $t_1 + t_3 - t_2$ or $t_3 + 0{\cdot}5\,t_1$. Since t_3 can be evaluated in terms of t_1, ratios of active carburizing time to total time can be obtained. Values of t_3 in terms of t_1, calculated from equation (6) by substituting various values of C_o and c_o, are shown in column (2) of the following data. Column (3) shows corresponding values of T, and in column (4) t_1 is shown as a fraction of the total carburizing time T:

Variation of the Ratio of Calculated Active Carburizing Period to Total Time, with Different Values of C_o and c_o, the Initial and Final Surface Carbon Concentrations for the Soaking Period, respectively

(1) Surface Carbon Potential after Soaking	(2) Hypothetical Total Time in Terms of t_1	(3) Total Time in Terms of t_1	(4) Ratio Active-Carb.-Period/ Total-Time
c_o, %	t_3	T	t_1/T
C_o = 1·5%			
1·0	0·91	1·41	0·71
0·9	1·13	1·63	0·61
0·8	1·43	1·93	0·52
0·7	1·86	2·36	0·42
0·6	2·54	3·04	0·33
0·5	3·65	4·15	0·24
0·4	5·70	6·30	0·16
C_o = 1·4%			
1·0	0·79	1·29	0·77
0·9	0·98	1·48	0·68
0·8	1·24	1·74	0·57
0·7	1·62	2·12	0·48
0·6	2·12	2·71	0·37
0·5	3·18	3·68	0·27
0·4	5·0	5·5	0·18
C_o = 1·3%			
1·0	0·69	1·19	0·84
0·9	0·85	1·35	0·74
0·8	1·07	1·57	0·64
0·7	1·40	1·90	0·53
0·6	1·91	2·41	0·41
0·5	2·74	3·24	0·31
0·4	4·29	4·79	0·21
C_o = 1·2%			
0·9	0·72	1·22	0·82
0·8	0·91	1·41	0·71
0·7	1·19	1·69	0·60
0·6	1·62	2·12	0·47
0·5	2·34	2·84	0·35
0·4	3·65	4·15	0·24
C_o = 1·1%			
0·9	0·61	1·11	0·90
0·8	0·76	1·26	0·79
0·7	1·00	1·50	0·67
0·6	1·36	1·86	0·54
0·5	1·96	2·46	0·41
0·4	3·06	3·56	0·28
C_o = 1·0%			
0·8	0·63	1·13	0·88
0·7	0·83	1·33	0·75
0·6	1·13	1·63	0·61
0·5	1·62	2·12	0·47
0·4	2·54	3·04	0·33

The values of t_1/T have been plotted against c_o in Fig. 3. As c_o approaches C_o, the calculated t_1 values become absurd because, as the soaking time approaches zero, the assumption that the character of the carbon diffusion changes instantaneously from that represented by equation (1) to that represented by equation (3), becomes more and more inaccurate. On the other hand, this assumption gives a better approximation as C_o and c_o diverge. In practice, it is usually desired to reduce the surface carbon by a substantial amount during diffusion. Up to $t_1 = 0{\cdot}65T$, the method has been shown to agree well with experimental results. The upper limit, beyond which t_1 is inaccurate, has not been established, but reasonable accuracy may be expected up to $t_1 = 0{\cdot}75T$, but not above $t_1 = 0{\cdot}85T$.

Examples of the usefulness of this method of

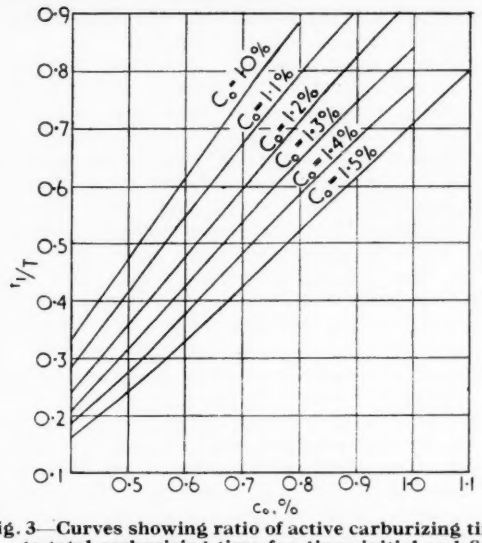


Fig. 3—Curves showing ratio of active carburizing time to total carburizing time for given initial and final surface carbon potentials

estimating active carburizing and soaking times are given in the following two calculations :

(1) Mild steel, 0·15% C. Carburizing temperature 925° C. Eutectoid-type case required—surface carbon concentration 1·0%. Carbon concentration in saturated austenite at 925° C., 1·45%.

$C_o = 1{\cdot}30\%$, $c_o = 0{\cdot}85\%$, t_1 (Fig. 3) $= 0{\cdot}69T$

(2) Low-alloy steel, 0·3% C. Carburizing temperature 925° C. Eutectoid-type case required—surface carbon concentration 0·8%. Carbon concentration in saturated austenite at 925° C., 1·45%.

$C_o = 1{\cdot}15\%$, $c_o = 0{\cdot}5\%$, t_1 (Fig. 3) $= 0{\cdot}38T$

In (1), the soaking time is less than one-third of the active carburizing time, whereas in (2), the soaking time is $1\frac{1}{2}$ times the active carburizing time. To arrive at the right conditions for such carburizations by a process of trial and error would be laborious.

CARBON PENETRATION CURVES FROM 'STRAIGHT' AND 'DIFFUSION-SOAK' CARBURIZATIONS

A comparison of penetration rates in diffusion-soak carburizations with those obtaining in straight carburizations is complicated by the fact that the distribution curves are dissimilar. It is misleading to make the comparison at any particular carbon potential because the penetration ratio varies with the carbon potential; the only satisfactory comparison is by complete penetration curves.

Hypothetical curves have been calculated by substitution in equations (1) and (5), and are shown in Figs. 4 and 5. In Fig. 4 the comparison is between a straight carburization for 100 hr., using a C_o value of 1·25% and a diffusivity coefficient of $1{\cdot}5 \times 10^{-7}$ (curve *a*), and three diffusion-soak carburizations of 100 hr. total carburizing time, with c_o values of 0·9, 0·7, and 0·5% (curves *b*, *d*, and *e*). Curves *b* and *e* of Fig. 4 are reproduced in Fig. 5, together with curves for straight carburizations for which the C_o values are 0·9 and 0·5 respectively.

Comparison of the penetrations shown in Fig. 4 is artificial; the surface carbon concentrations are not the same, and different types of case are represented. Whilst the penetrations measured to low values of C decrease steadily as the value of c_o decreases, penetrations measured to medium values of C are much less affected, at least until c_o is lowered to 0·5. A more interesting comparison is that given by the pairs of curves *b*, *f*, and *e*, *g*, in Fig. 5, where two methods of obtaining the same surface carbon concentration are illustrated. Whereas the penetrations to low C values are much the same, at high and medium values of C there is a pronounced difference between the two types of carburization, 'diffusion-soak' giving the higher values. Obviously, measurements of case depth to low C values can be misleading. The area enclosed by the carbon penetration curves, *i.e.*, the weight of carbon absorbed, offers a better basis for comparison than do single case-depth measurements. The ratio of these weights can be obtained as follows :

Wt. of carbon absorbed in straight carburization

$$= S_1 = 2c_o\sqrt{\frac{DT}{\pi}}$$

Wt. of carbon absorbed in diffusion-soak carburization

$$= S_2 = 2C_o\sqrt{\frac{Dt_1}{\pi}}$$

Whence

$$\frac{S_1}{S_2} = \frac{c_o}{C_o}\sqrt{\frac{T}{t_1}}$$

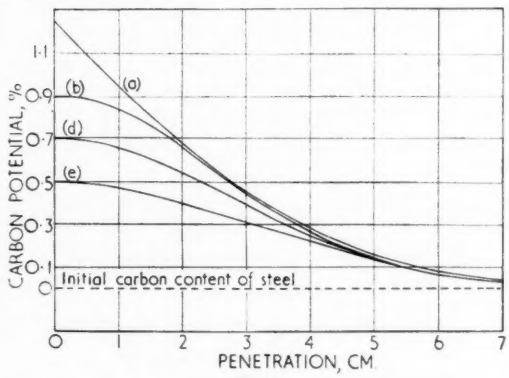


Fig. 4

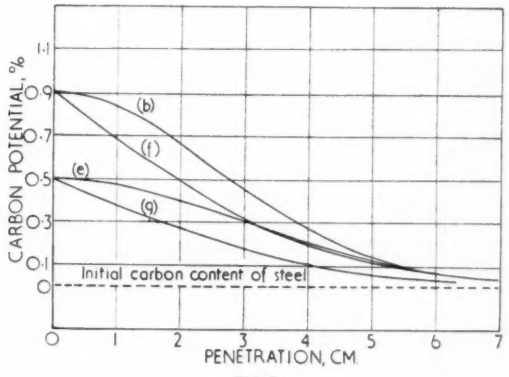


Fig. 5

Figs. 4 and 5—Comparison of 'straight' and 'diffusion-soak' carbon penetration curves

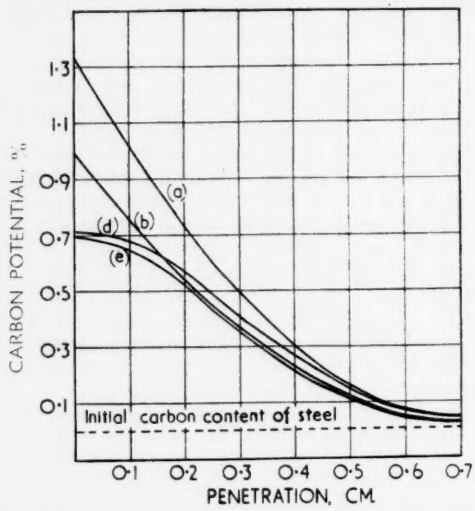


Fig. 6—Comparison of carburizations at different potentials

Over a fairly wide range, *e.g.*, $C_o = 1{\cdot}0$–$1{\cdot}5$ and $c_o = 0{\cdot}5$–$0{\cdot}9$, this ratio varies comparatively slightly *viz.*, between $0{\cdot}7$ and $0{\cdot}8$. For straight and diffusion-soak carburizations of equal duration and with the same final surface carbon concentration, the weight of carbon absorbed in the diffusion-soak is roughly one-third more than in the straight method.

In diffusion-soak carburizations, the penetration is given by the expression :

$$C = \frac{2C_o}{\pi}\sqrt{\frac{t_1}{t_3}} \cdot e^{-\frac{x^2}{4Dt_3}}$$

Conditions necessary for x to be proportional to $\sqrt{t_3}$ are that C, C_o, and t_1/t_3 are constant ; but t_1/t_3 is constant when C_o and c_o are constant ; furthermore, when C_o and c_o are constant, t_3 is proportional to t_1, t_1 is proportional to T, and therefore t_3 is proportional to T. Therefore when C, C_o, and c_o are constant, x is proportional to $\sqrt{T}$. In other words, if in a number of diffusion-soak carburizations of different durations, conditions are arranged so that C_o is the same in each experiment, and c_o is also the same, then the penetrations measured to the same carbon concentration C, will bear a linear relation to the square root of the total time.

Surface Carbon Potential and Carbon Penetration

The effect on diffusion of different values of the surface carbon potential C_o is of interest. The surface carbon potential depends upon the surface carbon concentration and the initial carbon concentration in the steel. In Fig. 6 four theoretical curves show the effect of surface carbon concentration during carburization of steels of the same initial carbon content. Curves *a* and *b* are for straight carburizations, and curves *d* and *e* are for diffusion-soak carburizations. In the first case the difference in penetration is not large at low values, but becomes proportionally greater at higher values of C. Curves *d* and *e*, on the other hand, are quite close to one another throughout, showing that the diffusion-soak carburization tends to minimize the effect of variations in surface carbon concentration.

Figure 7 shows two pairs of carbon penetration curves which illustrate the effect of different initial carbon concentrations in the steel, but with the same surface carbon concentration during carburization. Curves *a* and *b* again represent straight carburizations and *d* and *e* diffusion-soak carburizations. Here the diffusion-soak carburizations bear much the same relationship as the straight carburizations. Measured to the same carbon potential, *b* and *e* (low-carbon steel) show the greater penetrations. This shows how case-depth measurements on carburized steels of different initial carbon contents can be misleading.

Variation of Diffusivity Coefficient with Carbon Concentrations

It has been assumed that the diffusivity coefficient is constant at constant temperature. Wells and Mehl[4] showed that the diffusivity coefficient is a function of the carbon concentration. The foregoing deductions therefore require qualification. The analysis of the carburization process, taking into account the variation in diffusivity coefficient, would be too complicated for practical use, but its effect can be considered in general terms.

First consider the effect of the increase of the diffusivity coefficient with carbon concentration, on the application of equation (1). Values for D can be calculated from curves of this type by use of this equation, and provided that the same values of C_o and C are used, D will not vary. The value of D so obtained is not the true value for that particular concentration, but rather an integrated value for the concentration range from C_o to C. Equation (2) will also give values of D which do not vary, provided that the same surface carbon concentrations and the same initial carbon contents of the steel are used. The value of D obtained will also be an integrated

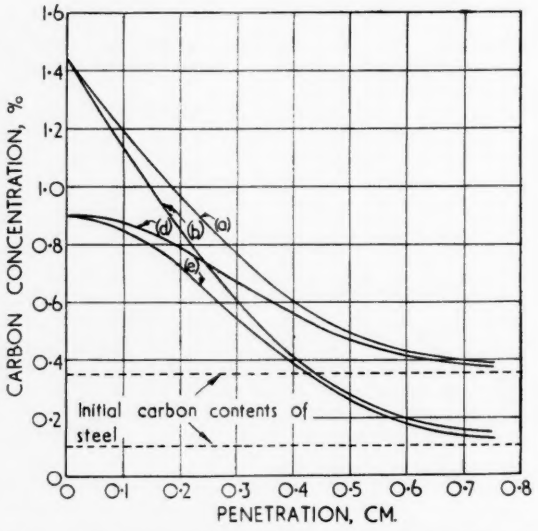


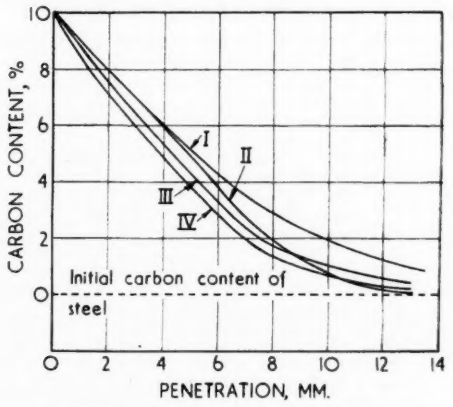
Fig. 7—Carbon penetration curves showing effect of different initial carbon contents of steel

average over the concentration range concerned. This is important, since in most gas carburizations surface carbon and initial carbon concentrations are approximately constant.

The increase of the diffusivity coefficient with carbon concentration means that the carbon penetration curve diverges somewhat from the hypothetical curves so far considered. For instance, if the areas under the curves are equal, *i.e.*, the hypothetical curve is that calculated by taking the average value of D satisfying equation (2), then the true penetration curve will show higher penetration values at the higher carbon concentrations, and lower penetrations at the lower carbon concentrations, compared with the hypothetical curve. Similarly, a curve calculated from the correct diffusivity coefficient at a high carbon concentration will give penetrations too high at the lower carbon concentrations, and a curve calculated from a D value, correct for a low carbon concentration, will give penetration values too low throughout. This is illustrated in Fig. 8, where an experimentally determined carbon penetration curve is compared with the three calculated curves. Figure 8 also illustrates the different shape of the true penetration curve compared with the hypothetical curves. A similar effect with penetration curves of the diffusion-soak type is illustrated in Fig. 9; comparison with the hypothetical curve, enclosing the same area only, is made.

The effect of this variation on the foregoing deductions may now be considered. That penetration is proportional to the square root of the time is strictly correct only when the surface carbon potentials are constant. The further proviso, that surface carbon concentrations must be constant, is now necessary, but of little moment, since in practice, constant surface carbon potential usually means that the surface carbon concentrations are the same.

The discussion on the rate of carbon absorption remains unaltered because, for the type of carburization considered, the carbon concentration range is the same throughout, and the average value of D in equation (2) is constant and the argument holds.



I—Calculated curve for D value at high carbon content
II—Experimental curve
III—Calculated curve for average D value
IV—Calculated curve for D value at low carbon content

Fig. 8—Curves illustrating effect of variations in diffusivity coefficient

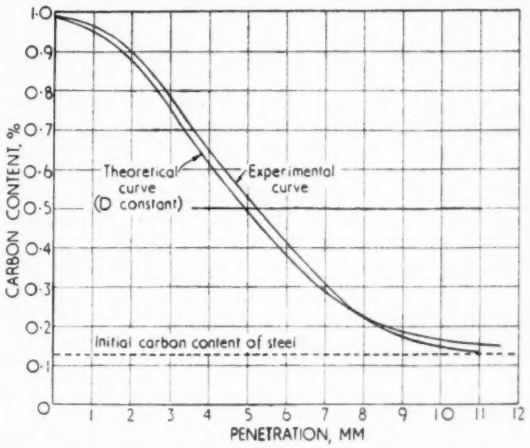


Fig. 9—Curves illustrating effect of variations in diffusivity coefficient

The variation of diffusivity coefficient with carbon concentration means that the actual straight carburization penetration curve will approximate more closely to the curve obtained on soaking than does the hypothetical straight carburization penetration curve. Consequently, the error introduced in the change-over from one type of curve to the other, at the end of the active carburizing period, is lessened. Comparison of the curves in Figs. 2 and 8 also shows why $0{\cdot}5t_1$ was preferred for the value of t_2. The calculation of t_3 is also affected by the variation in diffusivity coefficient. As the surface carbon concentration decreases during soaking, the diffusivity coefficient also decreases, so that equation (5) should really be :

$$C = \frac{2C_0}{\pi}\sqrt{\frac{t_1 D}{tD'}} \cdot e^{-\frac{x^2}{4D't}}$$

and equation (6) becomes :

$$t_3 = \frac{4}{\pi^2} \cdot \frac{C_0^2}{c_0^2} \cdot t_1 \frac{D}{D'}$$

where D' is less than D. Values of t_3 calculated on the assumption that D is constant, obviously tend to be too low, making T too low and t_1/T too high. Carburization times estimated from Fig. 3 should therefore give surface carbon concentrations slightly higher than those required. In practice, however, experimental and calculated results accord well, and it seems as if the slight loss of carbon which occurs during soaking (*see* Bramley and Lord,[3d] and Laidler and Taylor[6]) compensates for the error this factor introduces into the calculation. If the required reduction in surface carbon concentration was very large, this factor might become important, but for the usual diffusion-soak carburization this method of calculation is satisfactory.

The effect of the variation of D on the relationships shown in Figs. 4 and 5 is to increase the differences between the curves. In example a (Fig. 4) diffusion occurred with the carbon concentration gradient

always covering its full range, whilst in example *b*, in the latter stages of the carburization, diffusion occurred with progressively decreasing maximum carbon concentrations, and similarly in *d* and *e*. In example *a*, therefore, penetration values should be less, and the difference between *a* and *b* should be greater, and similarly for *d* and *e*, only more so. Likewise, in Fig. 5, diffusion in example *b* for part of the time was at a higher maximum carbon concentration than was obtained in example *f*. Penetrations in example *b* should therefore be greater, and the difference between *b* and *f* should be greater, and similarly for curves *e* and *g*, only more so. Except in the extreme examples, *a* and *e* in Fig. 4 and *e* and *g* in Fig. 5, the effect is small and may be neglected. The comparisons made earlier are thus accentuated, rather than obscured, by taking account of the variation of the diffusivity coefficient.

Assuming D to be constant, it was shown that penetration, measured to the same carbon potential in diffusion-soak carburizations employing the same values of C_o and c_o, will be proportional to the square root of the carburizing time. Just as in straight carburizations, this conclusion is not invalidated by the variation of D, provided that the carbon concentration range is the same in the different carburizations.

For each pair of curves in Fig. 6, *a*, *b*, and *d*, *e*, the effect of the variation of D will be to accentuate the differences. In Fig. 7, on the other hand, the relationships between the two pairs of curves should be practically unaltered. Down to the carbon content of the higher-carbon steel, curves *a*, *b*, and *d*, *e*, will be relatively unaltered. It is only below this carbon content that a lower diffusivity coefficient would slightly reduce the penetration shown in *b* and *e*, as compared with those in *a* and *d*.

The above deductions from the diffusion equations, D being assumed constant, are not substantially altered, therefore, by the variation of the diffusivity coefficient with the carbon concentration. Certain conclusions must be qualified slightly, but the general picture is unaltered.

Carburizations with Varying Surface Carbon Potential

For straight carburizations at constant surface carbon content, the carburizing conditions and results can be calculated with some accuracy from diffusion equations, but when the surface carbon is not constant this is impossible. In pack carburizations, the rate of absorption of carbon is a function of the carburizing temperature and the activity of the compound, and during a carburization it gradually diminishes, somewhat as pictured by the broken curve in Fig. 1. By comparison with the full curve, which represents carburization at a constant surface carbon content, it can be seen that initially the surface carbon concentration must be low, rising to a maximum, and beginning to decrease again towards the end of a long carburization. The art of successful pack carburization is to adjust time, temperature, and activity of compound so that the surface carbon content reaches the desired value at the end of the carburizing time.

Compared with efficient gas carburization, pack carburization is poorest for short carburizing times, when the possible rate of carbon absorption is very great. There is less to choose between the two methods at medium carburizing times, but again for long carburizing times exhaustion of the compound is a drawback in pack carburization. While the surface carbon concentration is increasing, the type of penetration curve is very similar to that given by equation (1), as shown by experiments of Bramley and Jinkings.[3b] What, in effect, is obtained is practically the same penetration curve as would result from a carburization in which the final C_o value had been maintained throughout, but for a shorter time. For long carburizations, in which the surface carbon concentration is decreasing, there will be a partial change-over to the diffusion-soak type.

This latter type is also obtained in much commercial gas carburization owing to sooting restricting the supply of carbon to the steel surface. If sooting is heavy, the curves given in Fig. 3, for evaluating active and soaking periods, cannot be used. Usually, however, sooting is light and these curves can be used, provided that the maximum value which has prevailed over most of the active carburizing period, and not the low figure obtaining at the end of the active carburizing period, is used for the surface carbon content. This would be the carbon concentration of saturated austenite, values for which have recently been critically reviewed by R. W. Gurry.[12] Gurry's values are applicable for steels of low alloy content, but other methods must be employed for medium-alloy steels, particularly those containing nickel. Because of the steep carbon gradient, direct determination of the carbon concentration of a carburized case is neither easy nor accurate. A method whereby it may be estimated from the carbon absorbed in layers of different thicknesses of the carburized case will be described in a further communication.

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A Comparison of Six Spring Steels

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Part I—HARDENABILITY AND RESISTANCE TO GRAIN GROWTH AND DECARBURIZATION

Introduction

ALTHOUGH a considerable amount of work has been done in the past on the effect of heat-treatment, surface defects, and decarburization on the fatigue strength of spring steels, numerous examinations of heat-treated silicon–manganese steel coil springs, which had failed prematurely in service during the recent war, generally showed that failure was due to one or more of the following causes :

(1) Surface decarburization, often severe, and resulting, in extreme cases, in the complete elimination of carbon to a depth of about 0·015 in. (Fig. 4)

(2) Bad surface finish due to heating to a too high temperature before coiling (Fig. 5)

(3) Ineffective heat-treatment, which occasionally resulted in complete lack of hardening of some coils of the spring (Fig. 6)

(4) Rolling laps and seams, which acted as severe stress raisers (Fig. 7).

Of these defects, (1), (2), and (3) were generally found, either alone or together, in coil springs which had been manufactured by the single-treatment process, whilst (4) was normally found, if present, in rectangular or trapezoidal section spring bar, which could not be dressed so easily as circular-section bar. Defects in springs made by the double-treatment process were not, in the experience of the Armament Research Establishment, very numerous, and such springs were usually of better quality than those made by the single-treatment method.

The single-treatment process refers to the method of utilizing a single heating, frequently to a temperature of about 1000–1100° C., for both coiling and subsequent quenching, whilst with the double-treatment process, a low temperature of the order of 800–850° C. is used for coiling and is followed by cooling and reheating to the correct hardening temperature. The final quenching temperature is, consequently, under complete control—in contradistinction to the conditions obtaining during hardening by the single-treatment process.

Because of the poor quality of many of the highly stressed heavy coil springs which were being supplied for gun recoil and vehicle suspension mechanisms, it was decided to undertake a fairly comprehensive investigation into the heat-treatment and properties of the four spring steels governed by specification S.T.A. 2, together with a chromium–molybdenum steel to specification S.T.A. 12, and a nickel–chromium–molybdenum steel to specification B.S. En 25. These two latter materials are not normally used for springs in this country, but were included because a steel similar to S.T.A. 12 has been tried for certain types of spring in the U.S.A., and one similar to B.S. En 25 was used in Russia for torsion-bar vehicle suspensions.

The steels used, with the exception of the nickel–chromium–molybdenum composition (5·5-in. square billet), were obtained as high-quality bar material, in various commercial conditions, 1·215–1·40 in. in dia. The chemical compositions of the materials are given in Table I. Apart from certain minor instances, and excluding steel Si–Cr (1), the steels listed complied with their respective specification requirements.

Macroscopic examination by sulphur printing and etching showed that all the materials were sound and free from any major segregation. The steels were all of average inclusion content, though in the as-received condition they possessed microstructures varying from as-rolled to sub-critically annealed. In view of these differences in the state of the carbides in the various steels, samples of the bar material were given a preliminary normalizing treatment (1 hr. at 900° C. followed by air-cooling) before use. Samples cut from the billet of Ni–Cr–Mo steel were used in the as-received (annealed) state owing to the air-hardening tendencies of this steel.

The normalized bars were machined to a standard diameter of 1·15 in. to remove any surface defects and decarburization, and were subsequently cut into 2·5-in. lengths for test purposes. Similar specimens were machined from the 5·5-in. square billet of Ni–Cr–Mo steel.

RESISTANCE TO DECARBURIZATION

As surface decarburization is one of the major factors influencing the life of a highly stressed spring, it will be clear that a desirable characteristic of a spring steel is good resistance to decarburization under the conditions obtaining in many commercial spring plants, where furnace-atmosphere control is not attempted, and where the spring bar is frequently heated to an unnecessarily high temperature.

To obtain, therefore, a relative measure of the susceptibility of the various steels to decarburization, 2·5-in. lengths of the normalized and machined bars were heated, in a vertical position, in a gas-fired muffle furnace, without atmosphere control, for

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Table I

CHEMICAL COMPOSITIONS OF STEELS USED

Steel	Specification	C, %	Si, %	Mn, %	S, %	P, %	Ni, %	Cr, %	Mo, %	V, %
Carbon	S.T.A. 2*a*	0·85 0·8–1·0	0·14 0·4 max.	0·76 0·35–0·70	0·060 0·05 max.	0·032 0·05 max.	0·42 ..	0·19 ..	.. ..	.. ..
Si–Mn	S.T.A. 2*b*	0·59 0·5–0·6	1·64 1·5–2·0	1·10 0·7–1·0	0·049 0·05 max.	0·040 0·05 max.	0·28 ..	0·18 ..	.. ..	.. ..
Cr–V	S.T.A. 2*c*	0·42 0·45–0·55	0·24 0·5 max.	0·62 0·5–0·8	0·047 0·05 max.	0·035 0·05 max.	0·38 ..	0·95 0·8–1·2	.. ..	0·18 0·15 min.
Si–Cr (1) Si–Cr (2)	S.T.A. 2*d*	0·56 0·60 0·5–0·6	0·85 1·50 1·4–1·6	0·38 0·69 0·6–0·9	0·036 0·030 0·05 max.	0·029 0·023 0·05 max.	.. 0·42 ..	0·91 0·65 0·6–0·8	.. .. ..	.. .. ..
Cr–Mo	S.T.A. 12	0·55 0·5–0·6	0·22 0·35 max.	0·38 0·3–0·6	0·034 0·035 max.	0·032 0·035 max.	0·30 ..	1·06 1·0–1·3	0·31 0·25–0·35	.. ..
Ni–Cr–Mo	B.S. En 25	0·35 0·27–0·35	0·23 0·1–0·35	0·54 0·5–0·7	0·029 0·05 max.	0·029 0·05 max.	2·37 2·3–2·8	0·68 0·5–0·8	0·29 0·4–0·7	.. ..

1 hr. at 900, 950, 1000, and 1050° C. A batch of specimens, consisting of one sample of each composition, was heated at any one temperature in order to obtain, as nearly as possible, identical treatment. The conditions used in this test were not so severe as those frequently met in industrial furnace heating, in which the bar stock may be exposed to direct flame impingement.

After air-cooling from the heating temperature, the various samples were sectioned transversely at the mid-length position, polished, and examined microscopically, with the following results:

Unetched—At all test temperatures from 900° to 1050° C. the Si–Mn and Ni–Cr–Mo steels showed intergranular oxide penetration, which was also present, though to a far less extent, in the Cr–Mo steel heated to 1050° C. This intergranular surface oxidation produced in Si–Mn steel after heating for 1 hr. at 1000° C. is illustrated in Fig. 8, which also shows the surface roughening produced, apparently by the detachment of complete grains of steel. Similar effects in Si–Mn steel have been noted by Andrew and Richardson[1] and by other workers,[2] and have also been seen by the authors in commercially heat-treated coil springs made by the single-treatment process (Fig. 9).

The depth to which this intergranular oxidation was visible in the unetched state increased, with rise in temperature of heating, to maximum values of 0·001 in. and 0·002 in. for the Si–Mn and Ni–Cr–Mo steels respectively.

The carbon, Cr–V, and Si–Cr steels showed none of this type of surface oxidation even after heating at 1050° C. for 1 hr.

Etched—The extent of the decarburization produced was measured microscopically, at the extremities of two mutually perpendicular diameters, on etched transverse samples of the various steels, with the results shown in Fig. 1*a*.

Under the experimental conditions of the decarburization test, the Si–Mn steel was most affected by oxidizing atmospheres in the temperature range 900–1050° C., the amount of decarburization increasing from 0·005 in. at 900° C. to 0·03 in. at 1050° C. None of the other steels was noticeably affected at 900° C., and maximum resistance to decarburization at temperatures up to 1000° C. was shown by the Cr–V material (0·001 in. at 1000° C.). Above this temperature, however, there was a rapid increase in the rate of decarburization which amounted to 0·013 in. after heating for 1 hr. at 1050° C.

A comparison between the behaviour of the two Si–Cr steels is interesting in that it shows the adverse effect of high silicon content on resistance to decarburization, an effect which has been noticed by other workers, *e.g.*, Burns,[3] and Johansson and von Seth.[4] The susceptibility to decarburization of the Si–Cr (2) (1·50% Si) steel was approximately double that of the Si–Cr (1) (0·85% Si) steel, although there were other differences in composition apart from silicon content.

Of the other samples tested, the Ni–Cr–Mo and Cr–Mo steels gave somewhat similar results up to 1000° C. (0·011 in.), but above this temperature, decarburization of the latter steel progressed rapidly,

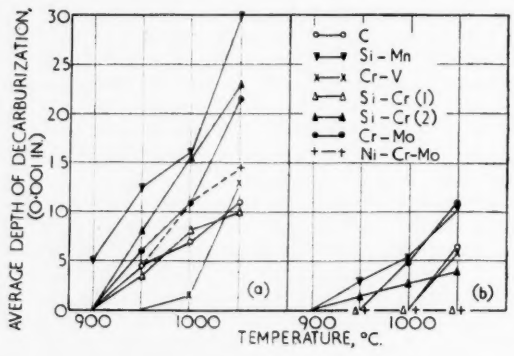

Fig. 1—Effect of temperature on decarburization of spring steels. Specimens heated in gas-fired muffle for 1 hr., and air-cooled. (*a*) Total depth of decarburization; (*b*) depth of carbon-free zone.

reaching a value of 0·021 in. at 1050° C. The carbon steel gave results almost identical with those obtained from Si–Cr (1) steel.

In addition to the partial decarburization shown, some of the samples exhibited a surface zone which was almost completely ferritic (Fig. 1*b*). The two steels which showed this effect most clearly were the Si–Mn and Cr–Mo varieties, the amount of surface total decarburization at 1050° C. being 0·010 and 0·011 in. respectively.

The carbon steel and the Cr–V steel did not produce any ferritic skin until a temperature of 1000° C. had been exceeded, whilst the Si–Cr (1) (0·85% Si) steel and the Ni–Cr–Mo steel did not show any surface total decarburization, even after heating to 1050° C. The Si–Cr (2) (1·50% Si) steel showed a gradual linear increase in surface total decarburization as the temperature of heating increased beyond 900° C.

The surface decarburization produced on typical samples of the various steels by heating for 1 hr. at 1000° C. is shown in Fig. 10.

RESISTANCE TO GRAIN GROWTH

As grain size may have a considerable effect on the notch sensitivity of steels, it was thought desirable to determine the effect on grain size of heating samples of the various steels at temperatures up to 1050° C. In an attempt, however, to measure the relative grain growth caused by heating for 1 hr. at various temperatures, several specified grain-size etching reagents and various techniques were employed with little success, and finally a modified McQuaid-Ehn test was adopted.

Specimens of the various steels were carburized in charcoal plus 10% of barium carbonate for 8 hr. at temperatures of 900, 950, 1000, and 1050° C. After furnace-cooling, they were polished and etched in boiling sodium picrate to reveal the hyper-eutectoid carbide at the grain boundaries. It is interesting to note that the Si–Mn steel could not be carburized by this method nor by the usual proprietary carburizing salt baths, and the grain size could not, therefore, be determined with any degree of accuracy. It was realized, of course, that the prolonged soak at temperature bore little relation to the normal heating of spring bars in practice, but it was thought that the results would be, at least, comparative.

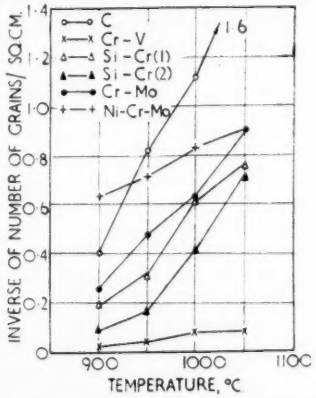

Fig. 2—Effect of temperature on grain size of spring steels. Specimens carburized for 8 hr. and furnace cooled. Number of grains/sq. cm. visible at a microscope magnification of 100 dia.

The results of these experiments are shown graphically in Fig. 2, in which the inverse of the number of grains per square centimetre of field (magnification × 100 dia.) is plotted against temperature of heating. The graphs illustrate clearly the different characteristics of the various steels as regards rate of grain growth on heating to 1050° C., the carbon steel showing the greatest rate, and the Cr–V steel the least. Next, in ascending order, came the Ni–Cr–Mo material, followed by Si–Cr (2) (1·50% Si), Si–Cr (1) (0·85% Si), and Cr–Mo steels.

HARDENABILITY

The industrial manufacture of heavy coil springs involves one of two techniques:

(1) The single-treatment process whereby the bar material may be heated to a relatively high temperature (up to 1050° C.), hot-coiled, and subsequently quenched from the mandrel without reheating

(2) The double-treatment process in which the bar material is coiled at a relatively low temperature (*circa* 850° C.), air-cooled, and then reheated to the correct hardening temperature before quenching.

The second process, by the use of comparatively low temperatures, avoids severe surface decarburization and grain growth, with their concomitant ill effects on fatigue resistance and notch sensitivity, and, in addition, enables standardized hardening to be adopted. The single-treatment process requires less furnace equipment, but tends to suffer from lack of control over the hardening temperature, and to produce coarse-grained, decarburized products.

The series of experiments herein described was designed to simulate to some extent the conditions obtaining in the manufacturing methods outlined above.

Before investigating the effects of varying times and temperatures of soaking on the depth-hardening characteristics of the materials under review, determination of the critical points was made by means of a plotting chronograph. The results obtained are given in Table II, and show the sluggish nature of the transformation in the Cr–Mo and the Ni–Cr–Mo materials.

Hardening Treatments

Samples, 2½ in. long, of the normalized material (1·15-in. dia.) were oil-hardened after the following treatments:

Series 1: 30 min. soak at 850, 900, 950, 1000, and 1050° C.
Series 2: 90 min. soak at 850, 900, 950, 1000, and 1050° C.
Series 3: 30 min. soak at 1000° C., followed by 20 min. at 800, 850, 900, and 950° C.
Series 4: 30 min. soak at 1050° C., followed by 20 min. at 800, 850, 900, and 950° C.

Series 1 and 2 were arranged to provide information on the effect of soaking time and temperature on hardenability under conditions similar to those of the double-treatment process, while series 3 and 4 simulated conditions of cooling and quenching from a single heat.

Heating for hardening was carried out in wire-wound electric furnaces in which a neutral atmosphere was maintained in order to obviate surface de-

Table II
CRITICAL CHANGE POINTS

Steel	Specification	Temperature, °C.				Cooling Rate, °C./min.
		Ac_1	Ac_2	Ar_1	Ar_2	
Carbon	S.T.A. 2*a*	730	...	681–669	...	6·6
Si–Mn	S.T.A. 2*b*	772	747	704–687	...	6·8
Cr–V	S.T.A. 2*c*	768 766	756 756	709 716	723 731	8·4 1·7
Si–Cr (1) Si–Cr (2)	S.T.A. 2*d* ,,	770 778	... ...	724–709 722–708	... ...	7·3 7·4
Cr–Mo	S.T.A. 12	764 763	... ...	481 710–705	... ...	4·4 1·9
Ni–Cr–Mo	B.S. En 25	734	...	478	...	4·3

carburization, and the specimens were allowed 20 min. to attain furnace temperature before the soaking period was commenced. For the heating of series 3 and 4, two juxtaposed furnaces, maintained at the requisite temperatures, were used, and preliminary tests indicated that specimens soaked at 1050° C. reached the required minimum temperature (800° C.) of the second furnace within 20 min.

Hardened specimens were finally slit transversely at mid-length, and after normal preparation, the section was hardness tested along a diameter, using a Vickers machine with a 30-kg. load.

With the exception of the carbon steel, which could not be hardened effectively throughout its cross-section, all the alloy steels were completely martensitic when oil-quenched from within the range of temperature used. In this respect, the results obtained with the Si–Mn steel differed widely from some obtained with similar samples of a steel which contained 2·15% of silicon and 0·83% of manganese.

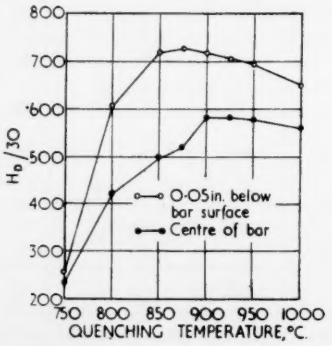

Fig. 3—Effect of quenching temperature on hardness of silicon–manganese steel to specification S.T.A. 2*b*. Oil-quenched after 60 min. soak. Specimens 2·5 in. long × 1·15 in. dia., sectioned transversely at mid-length position for hardness determination. Carbon 0·58%, Si 2·15%, Mn 0·83%

This steel could not be hardened throughout the section and was extremely sensitive to slight variations in hardening temperature around 850° C. (Fig. 3).

Microscopical Examination

The transverse sections used for hardness testing were prepared for microscopical examination and subsequently etched in a solution of 1% nitric acid in alcohol. The results were as follows:

Carbon Steel—All the specimens in series 1 to 4 were martensitic at the periphery of the transverse sections, and the depth of the martensite zone increased appreciably with increase in quenching temperature. It did not, however, extend to the centre of the bar with any of the quenching conditions employed.

Silicon–Manganese Steel—As would be expected from the hardness tests, the microstructures of all samples in series 1 to 4 were uniformly martensitic throughout the section, and in this respect differed considerably from the sample of Si–Mn steel mentioned earlier (*see* Fig. 3), in which ferrite was present at the centre of all specimens quenched from temperatures between 750° and 1050° C. In this connection, Burns[3] has recorded that a steel containing the maximum silicon and minimum manganese permitted by specification S.T.A. 2*b* exhibited a structure consisting of martensite and ferrite after oil-hardening.

Chromium–Vanadium Steel—In series 1 and 2, oil-hardening from 850–900° C. resulted in a relatively fine-grained martensitic structure in which a small amount of free ferrite was present. Hardening from over 900° C. and indirect quenching (series 3 and 4) gave a rather coarser-grained structure, which was completely martensitic.

Silicon–Chromium Steel (0·85% Si)—The hardened structure was uniformly martensitic, except for isolated traces of acicular ferrite in the centres of all specimens quenched from 850–900° C. Increasing the time of soak and raising the quenching temperature resulted in completely martensitic structures.

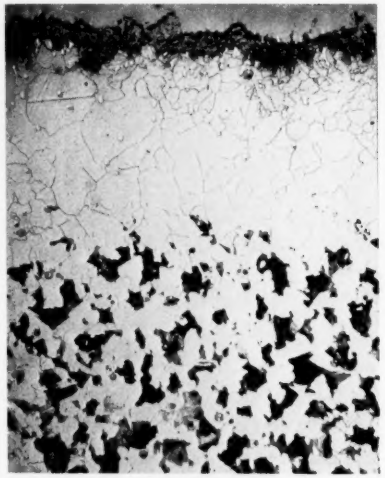

Fig. 4—Complete surface decarburization to a depth of 0·015 in. in a commercially heat-treated coil spring × 100

Fig. 5—Bad surface, due to overheating, of a commercially heat-treated coil spring × 1

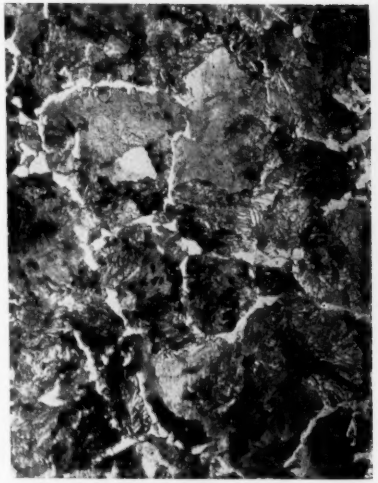

Fig. 6—Microstructure of section from a commercially heat-treated spring quenched below critical point × 500

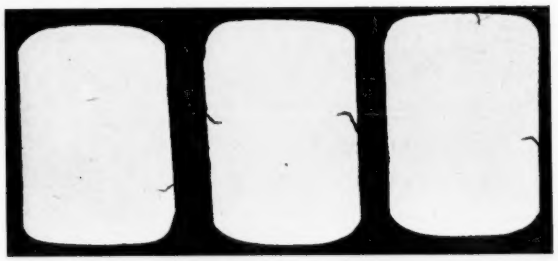

Fig. 7—Rolling laps in sections taken from coil spring which failed prematurely × 1

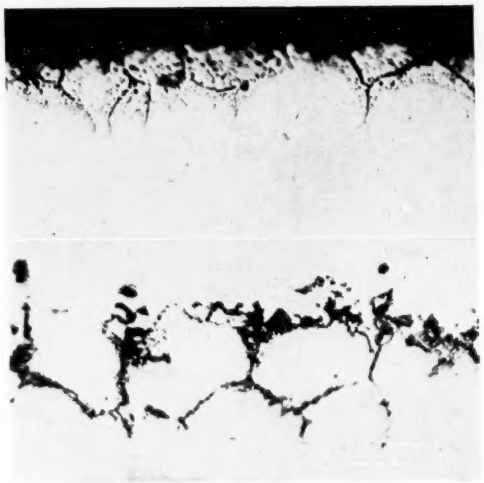

Fig. 8

Fig. 9

Fig. 8—Intergranular oxidation at surface, of silicon-manganese steel heated in air for 1 hr. at 1000° C. × 500

Fig. 9—Intergranular surface oxidation in commercially heat-treated silicon-manganese coil spring (nickel plated to preserve edge after sectioning)

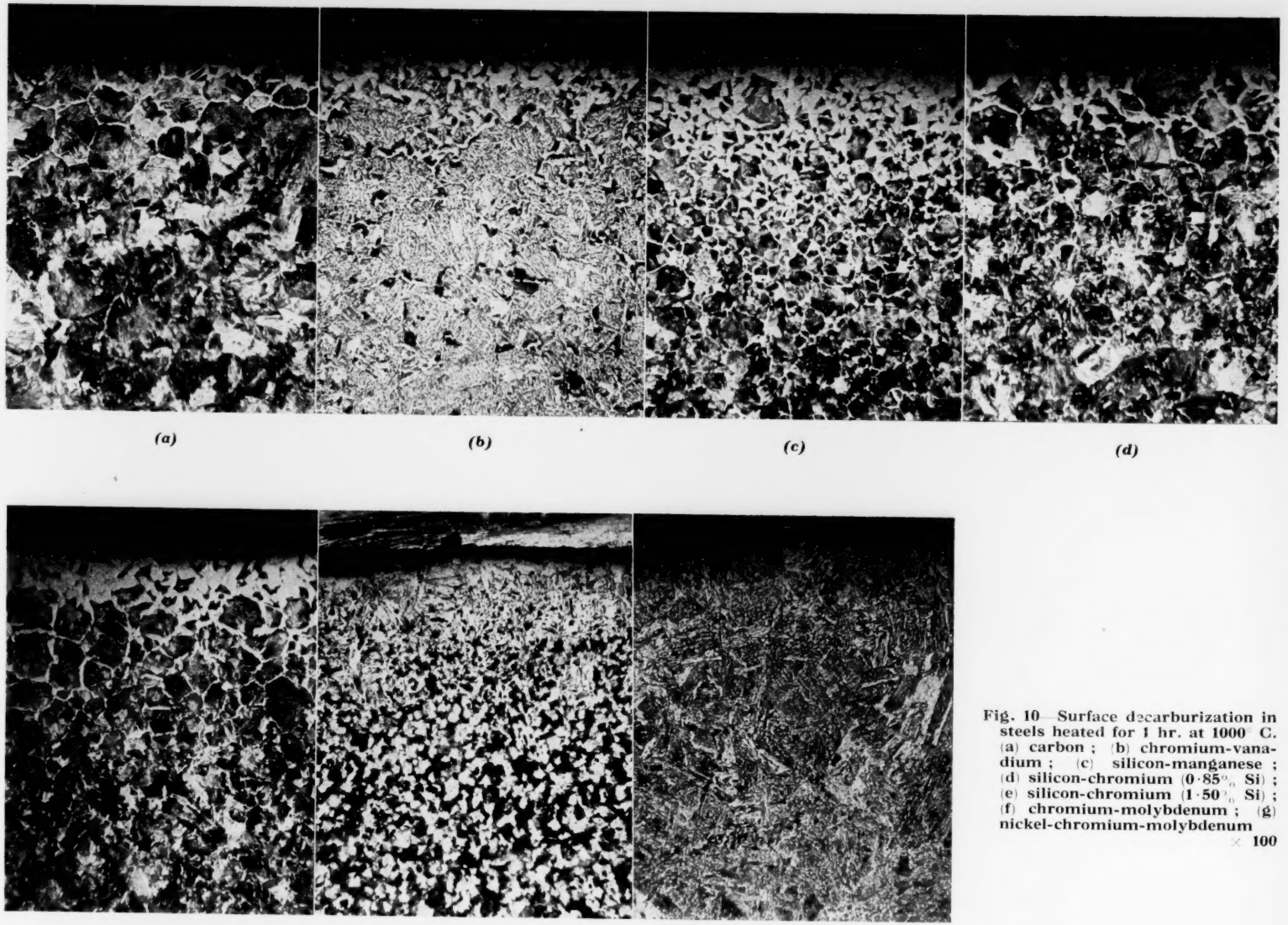

Fig. 10—Surface decarburization in steels heated for 1 hr. at 1000° C. (a) carbon ; (b) chromium-vanadium ; (c) silicon-manganese ; (d) silicon-chromium (0·85% Si) ; (e) silicon-chromium (1·50% Si) ; (f) chromium-molybdenum ; (g) nickel-chromium-molybdenum

× 100

Silicon–Chromium Steel (1·50% Si)—All specimens from series 1 to 4 exhibited a completely martensitic structure throughout the section. Quenching from temperatures in excess of 900° C., and preheating to 1000 or 1050° C. prior to quenching, merely resulted in grain growth.

Chromium–Molybdenum Steel—As expected from the hardness tests, the structures of all samples of this steel were completely martensitic, irrespective of the hardening treatments employed.

Nickel–Chromium–Molybdenum Steel—All samples of this steel after oil-quenching were completely martensitic.

Tempering Treatment

It is common practice to temper a spring after hardening in order to induce a certain amount of toughness and to relieve quenching stresses. This treatment is governed to some extent by specification S.T.A. 2, which stipulates a maximum hardness of 515 $H_D/30$, but which does not lay down any method of tempering. During the recent war, it was found that the methods of tempering employed by various firms ranged from adequate tempering for about 1 hr. in a furnace, to a few minutes in a lead bath, and springs have been examined in which the centre hardness was appreciably in excess of that at the surface, an effect caused by what might be termed a 'flash temper.'

In the present series of experiments, specimens $2\frac{1}{2}$ in. long and of 1·15 in. dia. were adequately tempered for 1 hr. at various temperatures in a salt bath or an oil bath (depending on the tempering temperature) after being hardened from the temperatures shown in Table III.

After hardening and tempering at various temperatures in the range 220–550° C., the specimens were slit transversely at mid-length and the transverse section prepared for hardness testing and subsequent microscopical examination.

The average results of hardness tests on these specimens are shown graphically in Figs. 11–17, in which average hardness numbers are plotted against tempering temperature.

Tempering for 1 hr. at the temperatures shown in Figs. 11–17 gave uniform hardness across the sections in all the samples tested. The effect on hardenability of changes in silicon and manganese contents within the limits allowed by specification S.T.A. 2*b* has already been pointed out (Fig. 3) and is further illustrated by Fig. 18, which shows the effect of tempering on hardened specimens of the steel which contained 2·15% of silicon and 0·83% of manganese. In this case the difference of some 160 $H_D/30$ between periphery and centre was reduced to about 60 $H_D/30$ after tempering at 400° C., and thereafter remained constant owing to the presence of free ferrite at the centre of the heat-treated bar.

The tempering characteristics of the various steels are summarized in Table IV, which shows that of the seven steels examined the Ni–Cr–Mo variety possessed the greatest resistance to tempering over the range 450–550 $H_D/30$. Next in order was the Cr–Mo material, which required an increase of 138° C.

Table III

HARDENING TREATMENT PRIOR TO TEMPERING

Steel	30 min. Soak and O.Q. from (° C.)	Average $H_D/30$ Periphery	Centre	Difference
Carbon	950	876	790	86
Si–Mn	900	769	769	0
Cr–V	900	633	554	79
Si–Cr (1)	900	752	692	60
Si–Cr (2)	900	804	786	18
Cr–Mo	900	733	725	8
Ni–Cr–Mo	850	559	564	−5

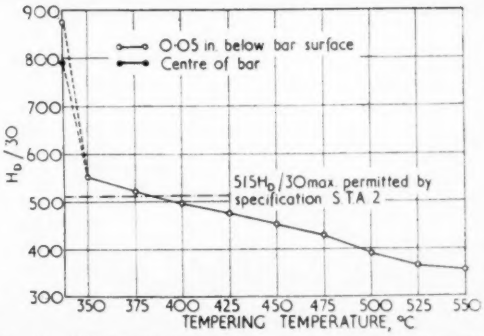

Fig. 11—Effect of tempering on hardness of carbon steel to specification S.T.A. 2*a*. Oil-quenched from 950° C. after 30 min. soak. Specimens 2·5 in. long × 1·15 in. dia., sectioned transversely at mid-length position for hardness determination. Hardness uniform over cross-section after tempering at 350° C. or over

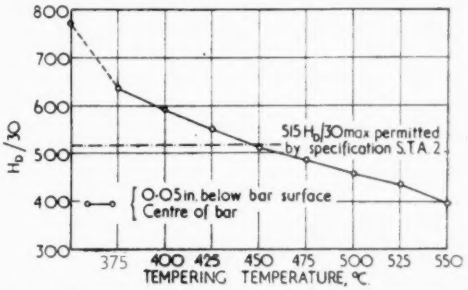

Fig. 12—Effect of tempering on hardness of silicon–manganese steel to specification S.T.A. 2*b*. Oil-quenched from 900° C. after 30 min. soak. Specimens 2·5 in. long × 1·15 in. dia. sectioned transversely at mid-length position for hardness determination

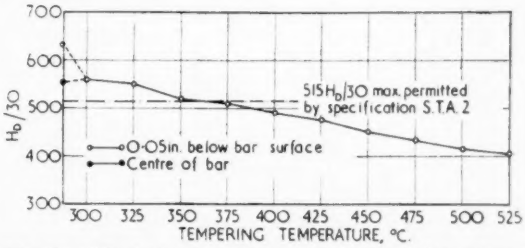

Fig. 13—Effect of tempering on hardness of chromium–vanadium steel to specification S.T.A. 2*c*. Oil-quenched from 900° C. after 30 min. soak. Specimens 2·5 in. long × 1·15 in. dia. sectioned transversely at mid-length position for hardness determination. Hardness uniform over cross-section after tempering at 300° C. or over

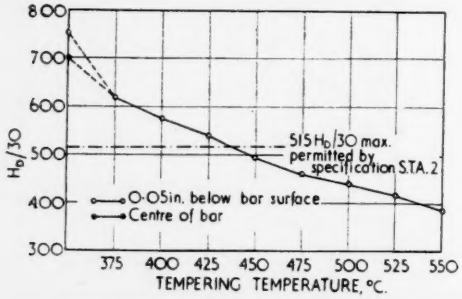

Fig. 14—Effect of tempering on hardness of silicon–chromium steel (0·85% Si) to specification S.T.A. 2. Oil-quenched from 900° C. after 30 min. soak. Specimens 2·5 in. long × 1·15 in. dia. sectioned transversely at mid-length position for hardness determination. Hardness uniform over cross-section after tempering at 375° C. or over

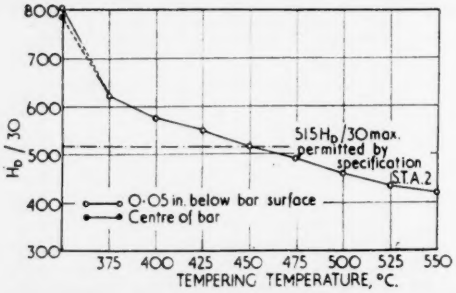

Fig. 15—Effect of tempering on hardness of silicon–chromium steel (1·50% Si) to specification S.T.A. 2. Oil-quenched after 30 min. soak. Specimens 2·5 in. long × 1·15 in. dia., sectioned transversely at mid-length position for hardness determination. Hardness uniform over cross-section after tempering at 375° C.

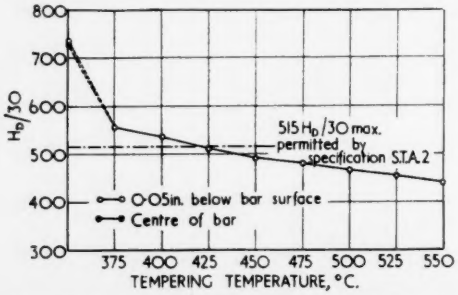

Fig. 16—Effect of tempering on hardness of chromium–molybdenum steel to specification S.T.A. 12. Oil-quenched from 900° C. after 30 min. soak. Specimens 2·5 in. long × 1·15 in. dia. sectioned transversely at mid-length position for hardness determination. Hardness uniform over cross-section after tempering at 375° C. or over

in tempering temperature for a reduction in hardness of 100 H_D/30. The Cr–V and carbon steels were also fairly resistant to tempering over the range of hardness examined, but whilst the former tempered only slowly from the hardened state, the latter showed a large decrease in hardness during the initial stages of tempering.

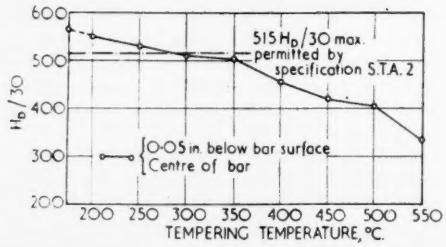

Fig. 17—Effect of tempering on hardness of nickel–chromium–molybdenum steel to specification En 25. Oil-quenched after 30 min. soak. Specimens 2·5 in. long × 1·15 in. dia. sectioned transversely at mid-length position for hardness determination. Hardness uniform over cross-section after tempering at 375° C. or over

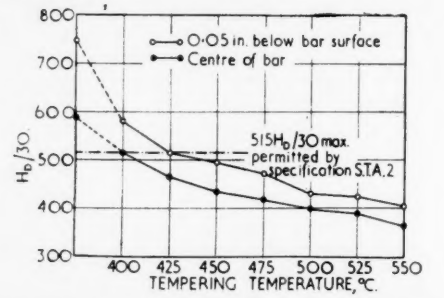

Fig. 18—Effect of tempering on hardness of silicon–manganese steel to specification S.T.A. 2*a*. Oil-quenched from 875° C. after 35 min. soak. Specimens 2·5 in. long × 1·15 in. dia. sectioned transversely at mid-length position for hardness determination. Carbon 0·58%, Si 2·15%, Mn 0·83%

The behaviour of the silicon-bearing steels, Si–Mn and Si–Cr was similar in that all three tempered fairly rapidly, requiring an increase in tempering temperature of only 75–80° C. for a 100 H_D/30 drop in hardness over the range 550–450 H_D/30.

SUMMARY AND CONCLUSIONS

Determination of the effect of temperature on the decarburization and grain growth of a series of spring steels, when heated for 1 hr. under conditions similar to those prevailing in many industrial furnaces, has shown that:

(1) Of the four steels governed by specification S.T.A. 2, namely, plain carbon, silicon–manganese, chromium–vanadium, and silicon–chromium, the silicon–manganese was the most susceptible to surface decarburization. Under the conditions

Table IV

TEMPERING CHARACTERISTICS OF STEELS OVER THE RANGE 550–450 H_D/30

		Tempering Temperature, °C. (1 hr.) for Hardness 450 H_D/30	550 H_D/30	Increase in Temperature, °C., for 100 H_D/30 drop
C	S.T.A. 2*a*	450	350	100
Si–Mn	S.T.A. 2*b*	505	425	80
Cr–V	S.T.A. 2*c*	450	325	125
Si–Cr (1)	S.T.A. 2*d*	495	420	75
Si–Cr (2)	S.T.A. 2*d*	500	425	75
Cr–Mo	S.T.A. 12	525	387	138
Ni–Cr–Mo	B.S. En 25	400	200	200

used, *i.e.*, a muffle furnace fired by town's gas and not having any protective atmosphere, the Si–Mn steel showed an intergranular type of surface oxidation, after heating at temperatures in excess of 950° C., which increased in severity with increasing temperatures until, at a temperature of 1050° C., whole grains became detached from the surface of the bar. This liability to surface decarburization can be appreciably accentuated by increase of silicon and decrease of manganese content within the limits allowed by specification S.T.A. 2*b*. At temperatures up to 1000° C., the Cr–V steel showed by far the least decarburization, whilst in the region of 1050° C., the plain carbon and low silicon Si–Cr steels showed least decarburization. The Si–Cr steel containing 1·50% Si, however, showed a susceptibility to decarburization approaching that of the Si–Mn steel.

Under similar conditions, the Cr–Mo steel to specification S.T.A. 12 was also badly decarburized, though not to the same extent as the Si–Mn composition.

The Ni–Cr–Mo steel behaved similarly to the Cr–Mo material up to temperatures of 1000° C., but above this temperature showed considerably less decarburization than the latter. Both steels were, however, subject to some extent to the intergranular surface oxidation noticed in Si–Mn steel.

(2) As regards grain growth on heating, the plain carbon steel was by far the worst of the four steels to specification S.T.A. 2. Si–Mn steel, whilst considerably better in this respect than the plain carbon variety, was not so good as the chromium-containing steels of which the Cr–V composition showed the least grain growth.

Oil-hardening and tempering experiments on the four spring steels permitted by specification S.T.A. 2, on a Cr–Mo steel to specification S.T.A. 12, and on a steel to specification B.S. En 25 have shown that:

(i) With the composition of steel used, the Si–Mn, Cr–Mo, and Ni–Cr varieties hardened easily throughout the section (1·15-in. dia.) when oil-quenched from temperatures in the range 850–1050° C. Following in order of hardenability as determined by total quenching were Si–Cr, Cr–V, and plain carbon steels. It was found impossible, under the experimental conditions used, to obtain this latter steel in a completely martensitic state throughout the section.

It must be remembered, however, that in the case of the alloy steels, allowable variations in composition within the specification limits can influence hardenability appreciably. This effect is particularly noticeable with the Si–Mn composition.

(ii) The method of quenching, *i.e.*, whether from soaking temperature or from a lower temperature following a high soaking temperature had little or no effect on the hardness of Si–Mn, Si–Cr, Cr–Mo, and Ni–Cr–Mo steels, but the hardenability of the Cr–V and carbon steels was improved by the use of a high soaking temperature (up to 1050° C.) followed by quenching from a considerably lower temperature (850–900° C.). This increase in hardenability with these two steels was, however, only obtained at the expense of an increase in grain size, which was considerable in the case of the plain carbon steel, and would be accompanied by some lowering of the impact value and fatigue resistance.

(iii) After tempering at temperatures in excess of about 375° C., oil-quenched specimens of each steel had a uniform hardness from edge to centre, irrespective of considerable differences existing in some of the steels before tempering.

(iv) Over the hardness range 450–550 $H_D/30$, the steel most resistant to tempering was the Ni–Cr–Mo material, followed in turn by the Cr–Mo and plain carbon varieties. The silicon-bearing steels, Si–Mn and Si–Cr, were characterized by having a much higher tempering rate, and required an increase of only 75–80° C. to produce a decrease in hardness from 550 to 450 $H_D/30$ under the experimental conditions used.

Part II—JOMINY HARDENABILITY AND MECHANICAL PROPERTIES AFTER HARDENING AND TEMPERING

In view of the increasing use of the end-quench hardenability test, the work described in Part I has been extended to include a series of experiments on the effect of increasing the quenching temperature on the results given by this type of test, and a study has also been made of the mechanical properties of the various materials after oil-hardening and tempering to give hardness values of 450 and 550 $H_D/30$. A few experiments have also been made on the effect of decarburization on the fatigue properties of the oil-hardened and tempered steels at the same hardness levels.

END-QUENCH HARDENABILITY

Standard size Jominy test pieces were machined from the normalized bar materials and end-quenched after 20 min. soak at 800, 850, 900, 950, 1000, and 1050 °C.

Heating for hardening was carried out in an electric muffle furnace in an atmosphere of dry town's gas in order to reduce surface decarburization. The test pieces were supported in the furnace in a cradle made from strip steel so that contact with the furnace lining was avoided, and they were allowed 30 min. to attain furnace temperature before the standard soaking period of 20 min. commenced.

Quenching took place under the standard conditions, using a water jet issuing from a ½-in. nozzle and having a free height of 2½ in.

The result of variation of quenching temperature is effectively illustrated in Figs. 19 and 21, which show the change in hardness at four positions, 0·25, 0·5, 1·0, and 2·0 in. from the quenched end of the specimen, plotted against hardening temperature.

Carbon Steel—The results given in Fig. 19*a* confirm that this steel has poor depth-hardening properties, as indicated by the oil-hardening experiments described in Part I. The hardenability of the material

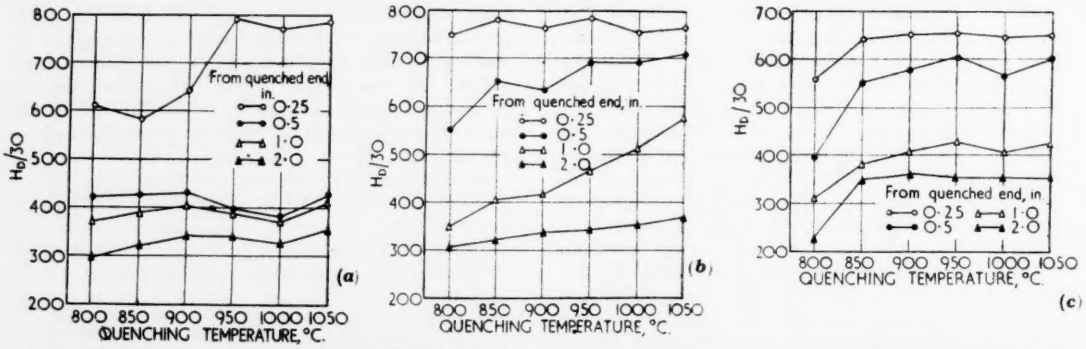

Fig. 19—Effect on hardenability of variation of quenching temperature: (*a*) carbon steel to specification S.T.A. 2*a*, (carbon 0·85%, Mn 0·76%); (*b*) silicon–manganese steel to specification S.T.A. 2*b* (carbon 0·59%, Si 1·64%, Mn 1·10%); (*c*) chromium–vanadium steel to specification S.T.A. 2*c* (carbon 0·42%, Cr 0·95%, V 0·18%)

is improved considerably by increasing the quenching temperature up to 950° C., beyond which point further increases result in a slight drop in the hardenability (Fig. 19*a*). It is, however, impossible in practice to take advantage of this improvement in hardenability because of the liability of this steel to show quenching cracks after hardening from temperatures above about 900° C.

Silicon–Manganese Steel—With a Si–Mn steel of the composition used (carbon 0·59%, Si 1·64%, and Mn 1·10%), an increase of quenching temperature from 800° to 1050° C. is virtually without effect on the hardness (750–775 $H_D/30$) at $\frac{1}{4}$ in. from the quenched end of the bar (Fig. 19*b*). This $\frac{1}{4}$-in. position corresponds approximately to the size of specimen (1·15-in. dia.) used in the previous oil-quenching experiments in which it was shown that variations in quenching temperature above 850° C. had little effect on the hardenability of the material.

Chromium–Vanadium Steel—The hardenability of the Cr–V steel increased with increasing quenching temperature up to 850° C., and thereafter was almost unaffected by further increases up to the maximum of 1050° C. used in these experiments (Fig. 19*c*). This result differs somewhat from those found from the previous oil-quenching experiments, in which the centre hardness of a 1·15-in. dia. bar gradually increased with increase of quenching temperature up to 1000° C., when it remained unaltered by an increase of quenching temperature to 1050° C.

Silicon–Chromium Steel (0·85% Si)—This sample, which did not conform to the silicon and chromium requirements of specification S.T.A. 2*d*, possessed relatively good depth-hardening properties. The hardness at the $\frac{1}{4}$-in. position was unaffected by any increase in quenching temperature above 850° C., and this confirmed the results previously obtained from experiments on oil-quenching from various temperatures.

Some of the hardness/distance curves for this steel showed an abnormality (Fig. 20) when quenching temperatures between 900° and 1050° C. were employed, though normal curves were obtained with quenching temperatures above and below this range. A similar phenomenon has been observed by Rowland, Welchner, and Marshall[5] in a high-carbon chromium steel of the ball-bearing type, by Williams[6] in a manganese–chromium steel, and subsequently by Pumphrey[7] in a high-carbon chromium steel.

With the present steel, it was observed that the hardness increase invariably occurred within the range 380–430 $H_D/30$, and extended over a distance (from the quenched end) which increased from $1\frac{3}{8}$–$1\frac{1}{2}$ in. with a quenching temperature of 900° C., to $1\frac{3}{4}$–$2\frac{1}{4}$ in. with a quenching temperature of 1050° C., *i.e.*, the hump in the curve gradually spread over a greater length of specimen and was displaced away from the quenched end as the quenching temperature increased.

Microscopical examination of test pieces end-quenched from 800° C. (normal curve), 950° C. (abnormal curve), and 1200° C. (normal curve) offered a possible explanation. When end-quenched from 950° C. the steel was completely martensitic (Fig. 22) at the quenched end. At a position $\frac{1}{4}$ in. away from the quenched end (B in Fig. 20) an intermediate transformation product resembling bainite appeared in the martensite matrix (Fig. 23), and increasing amounts of this constituent, appearing as the distance from the quenched end increased, presumably accounted for the steady fall in hardness along the bar. At a cooling rate corresponding to position *C* (Fig. 20), transformation, mainly to the intermediate product, took place, and resulted in the type of microstructure

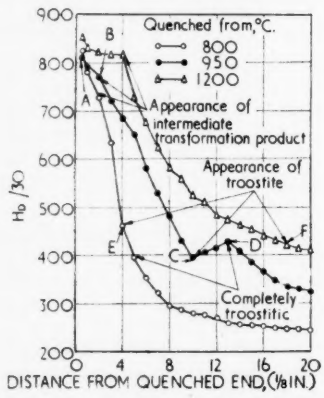

Fig. 20—Variation of hardness over the length of the specimen for different quenching temperatures. Silicon–chromium steel to specification S.T.A. 2*d* (carbon 0·56%, Si 0·85%, Cr 0·91%)

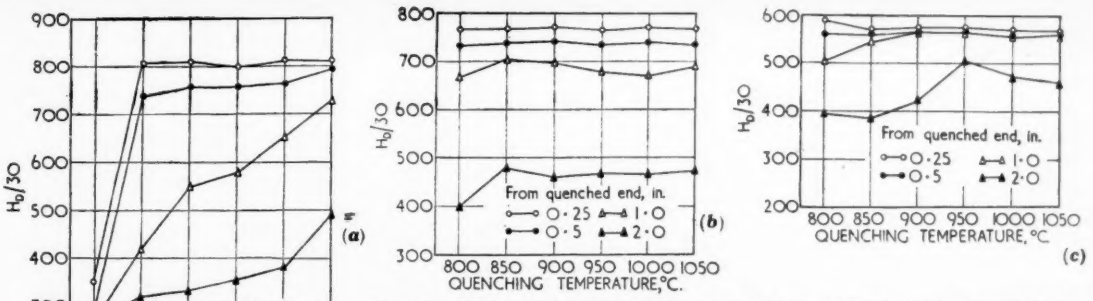

Fig. 21—Effect on hardenability of variation of quenching temperature: (*a*) silicon–chromium steel to specification S.T.A. 2*d* (carbon 0·6%, Si 1·50%, Cr 0·65%); (*b*) chromium–molybdenum steel to specification S.T.A. 12 (carbon 0·55%, Cr 1·06%, Mo 0·31%); (*c*) nickel–chromium–molybdenum steel to specification B.S. En 25 (carbon 0·35%, Cr 0·68%, Ni 2·37%, Mo 0·29%)

shown in Fig. 24, which shows the intermediate constituent plus a small amount of troostite. With a further decrease in cooling rate, larger amounts of troostite were formed until, at a cooling rate corresponding to *D* (Fig. 20), the structure was almost entirely troostitic (Fig. 25). Further decrease in cooling rate resulted only in a coarsening of the troostite and the appearance of increasing amounts of ferrite.

The changes in microstructure along the specimens end-quenched from 800° and 1200° C. were similar to those in the bar quenched from 950° C., with the difference, however, that the initial traces of troostite (positions *E* and *F* respectively in Fig. 20) appeared in a martensitic matrix containing less of the intermediate transformation product.

The unusual hardening effect observed in samples of this particular Si–Cr steel when end-quenched from temperatures between 950° and 1050° C. would appear to be associated with the hardness of the structure at the initial appearance of troostite, compared with the hardness of a totally troostitic structure (430 $H_D/30$ in this case). The formation of a structure having a hardness value greater than 430 $H_D/30$ results in a normal hardness/distance curve; whereas a hardness value below 430 $H_D/30$, produced by an abnormal amount of intermediate transformation product, is increased, further along the bar from the quenched end, by the formation of a troostitic structure, thus producing the observed abnormal hardness/distance curve.

Silicon–Chromium Steel (1·5% Si)—Figure 21*a* shows that the hardenability of this sample of Si–Cr steel, which conformed to specification S.T.A. 2*d*, was better than that of the lower silicon steel discussed above. An increase in quenching temperature from 850° to 1050° C. was without effect on the hardness at a position $\frac{1}{4}$ in. from the end of the bar, a result which agreed with the constant centre-hardness of 1·15-in. dia. bars oil-quenched in the same temperature range.

Chromium–Molybdenum Steel—Cr–Mo steel to specification S.T.A. 12 possessed good depth-hardening characteristics which, in the range of sections used for the types of spring under discussion (1·15-in. dia. max.), were virtually unaffected by variations in quenching temperature in the range 800–1050° C. (Fig. 21*b*)

Nickel–Chromium–Molybdenum Steel—Though the maximum hardness attained by a Ni–Cr–Mo steel to B.S. En 25 was only approximately 600 $H_D/30$, the steel possessed the best depth-hardening properties of the whole series of spring steels tested in the present experiments. The hardenability was almost unaffected by any increase in quenching temperature above 850° C. (Fig. 21*c*).

MECHANICAL PROPERTIES AFTER OIL-HARDENING AND TEMPERING TO 450 AND 550 $H_D/30$

As examination of many commercially made heavy coil springs had shown a wide range of hardness values, it was decided to examine the mechanical properties of the hardened and tempered spring steels at two levels, namely 450 $H_D/30$, considerably below the maximum of 515 $H_D/30$ permitted by specification S.T.A. 2, and 550 $H_D/30$ which, though considerably above the permitted maximum, was somewhat lower than the maximum of 570 $H_D/30$ found in some commercial springs.

The following mechanical tests (in duplicate) were carried out with each steel at the two hardness levels: (i) Tensile; (ii) shear; (iii) izod impact; (iv) torsion impact; (v) rotating cantilever fatigue.

All test pieces were heat-treated in controlled atmospheres in the rough machined condition, and then finally machined to size, in order to ensure freedom from decarburization. In addition, one series of fatigue specimens was purposely decarburized to a depth of about 0·005 in., in order to show the effect of decarburization on the fatigue strength of the various materials.

Tensile Tests

The hardened and tempered test pieces (dia. 0·2 in.) were tested in a 30-ton hydraulic machine, the yield point (0·2% proof stress) being measured by a Ewing extensometer. The results obtained are given in Table V.

Taking first the tests made on samples with a hardness of approximately 550 $H_D/30$, there was little difference either in yield point (90–110 tons/sq.in. average values) or in ultimate strength (114–120 tons/sq. in. average values) of the various types of steel. The major differences occurred in the values obtained

Table V

MECHANICAL PROPERTIES OF SPRING STEELS AFTER HARDENING AND TEMPERING TO GIVE 450 AND 550 $H_D/30$

Steel	Specification	Heat-Treatment	Yield* tons/sq. in.	U.T.S., tons/sq. in.	Elong., %	R. in A., %	Hardness (Tensile)	Max. Shear Stress, tons/sq. in.		Hardness (Shear)
Carbon	S.T.A. 2*a*	O.Q. 825° C.	...†	...	...	...	557	94·7	Av. 93·1	561
		T. 350° C.	...†	...	...	...	557	91·6		557
		O.Q. 825° C.	84·0	97·5	9·5	21	457	81·0	Av. 81·0	454
		T. 450° C.	87·0	100·25	7·5	17	457	81·0		454
Si–Mn	S.T.A. 2*b*	O.Q. 850° C.	113·0	123·75	9·0	32·5	540	96·7	Av. 97·3	564
		T. 425° C.	106·0	116·75	10·5	29	543	98·0		551
		O.Q. 850° C.	86·0	95·5	13	29	446	80·3	Av. 80·3	446
		T. 505° C.	86·5	95·75	10	24	449	80·3		446
Cr–V	S.T.A. 2*c*	O.Q. 850° C.	97·65	110·25	13	48	543	88·7	Av. 90·2	560
		T. 325° C.	100·0	113·5	12	43	547	91·6		560
		O.Q. 850° C.	85·5	90·25	14	52	447	74·0	Av. 74·3	454
		T. 450° C.	85·5	90·5	14	52	449	74·7		457
Si–Cr (1) (0·85% Si)	S.T.A. 2*d*	O.Q. 850° C.	100·5	114·0	7·5	24	540	96·0	Av. 97·0	554
		T. 415° C.	102·0	114·0	7·5	24	543	98·0		557
		O.Q. 850° C.	84·0	92·25	12	34	451	82·0	Av. 82·3	457
		T. 500° C.	83·5	92·7	11	30	449	82·7		459
Si–Cr (2) (1·50% Si)	S.T.A. 2*d*	O.Q. 850° C.	103·0	114·0	12	43	550	94·4	Av. 94·4	547
		T. 425° C.	103·9	114·85	11	37	547	94·4		550
		O.Q. 850° C.	86·25	95·0	12	37	457	82·2	Av. 81·8	457
		T. 510° C.	85·0	94·25	12	37	454	81·5		454
Cr–Mo	S.T.A. 12	O.Q. 850° C.	...†	...	...	...	555	92·0	Av. 92·3	554
		T. 375° C.	...†	...	...	...	557	92·7		554
		O.Q. 850° C.	86·0	95·0	13	34	457	80·7	Av. 80·7	449
		T. 525° C.	89·0	97·5	12	40	462	80·7		449
Ni–Cr–Mo	B.S. En 25	O.Q. 850° C.	90·0	115·8	12	46	550	97·3	Av. 96·7	550
		T. 200° C.	90·0	114·2	12	48	550	96·0		550
		O.Q. 850° C.	80·0	90·3	14	52	446	75·3	Av. 74·5	449
		T. 400° C.	80·0	89·75	13	52	454	73·8		449

* Stress to give 0·2% permanent extension. † Broke in grips before yield point was reached.

for the reduction in area at fracture, which varied from 24% for the Si–Cr (0·85% Si) steel to 47% for the Ni–Cr–Mo steel. The value given by the most commonly used spring steel, Si–Mn, was intermediate between the two extremes quoted above, being 31%.

Results were not obtained on two steels (carbon and Cr–Mo), the test specimens of which broke in the screwed grips before the yield point (0·2% permanent extension) had been reached.

Similar remarks apply to the samples tempered, after oil-hardening, to give a hardness in the region of 450 $H_D/30$. There was little difference depicted in yield points (80–86 tons/sq. in. average values) or in tensile strength (90–99 tons/sq. in. average values). At this hardness level, the carbon steel showed least ductility, whilst the Cr–V and Ni–Cr–Mo steels were the best in this respect.

Shear Tests

Shear-test specimens 0·252 in. dia. and 4·5 in. long were machined and heat-treated in the same way as the tensile test pieces to give hardnesses of 550 and 450 $H_D/30$.

The specimens, which were in duplicate for each hardness level, were tested in double shear, and gave the results shown in Table V.

No important differences between the various steels, whether tempered to 550 or 450 $H_D/30$, were revealed by shear tests.

Izod Impact Tests

Two triple-notch izod impact test pieces from each steel were oil-hardened and tempered to give hardness values of 550 and 450 $H_D/30$ respectively. The heat-treatment applied was similar to that given to the tensile test pieces (*see* Table V.)

Table VI shows the results obtained from these tests, which reveal considerable differences in the izod impact properties of the various steels. The carbon steel, in particular, gave very low values after oil-hardening and tempering to 550 and 450 $H_D/30$.

Of the other steels tested, the highest impact value of 13·5 ft.lb. at 550 $H_D/30$ was obtained from the Ni–Cr–Mo steel, followed by 7·5 ft.lb. for the Si–Cr material containing 1·50% Si. The other sample of Si–Cr steel which was lower in silicon (0·85%, against 1·50%) and manganese (0·37%, against 0·69%) gave much lower values (3 ft.lb.) when hardened and tempered to give the same hardness of 550 $H_D/30$. The notched bar impact strengths of the remaining steels (Si–Mn, Cr–V, and Cr–Mo) were all low and ranged from 2·5 to 5 ft.lb.

Tempering of the oil-hardened test pieces to give a lower $H_D/30$ of 450 resulted in an increase of izod value of all the steels exept the Ni–Cr–Mo variety, in which the energy to fracture fell from 13·5 ft.lb. at 550 $H_D/30$ to 9 ft.lb. at 450 $H_D/30$. As, however, the izod-impact-value/tempering-temperature curve for steel of this composition shows a marked drop

Fig. 22—Martensitic structure of quenched end of Jominy test piece of silicon-chromium steel

Fig. 23—Martensite and bainite at ¼ in. away from quenched end (*B* in Fig. 20)

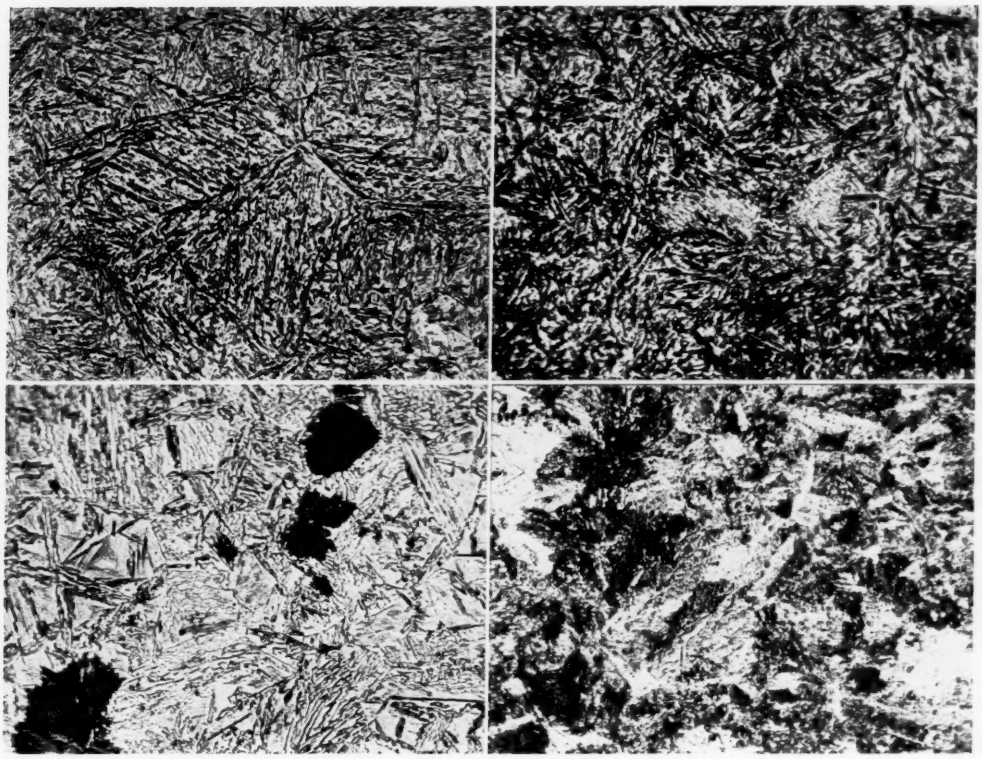

Fig. 24—Structure at position *C* in Fig. 20, mainly bainite

Fig. 25—Troostite structure at position *D* in Fig. 20

Table VI

IZOD IMPACT PROPERTIES OF HEAT-TREATED SPRING STEELS

Steel	Specification	Izod Impact, ft.lb.				$H_D/30$
		1	2	3	Average	
Carbon	S.T.A. 2*a*	0·5	1·0	1·0	0·8	557
		1·0	1·5	1·5	1·3	457
Si–Mn	S.T.A. 2*b*	2·5	3·0	3·5	3·0	546
		5	4	4	4	454
Cr–V	S.T.A. 2*c*	5	5	5	5	550
		15	13·5	13·5	14	446
Si–Cr (1) (0·85% Si)	S.T.A. 2*d*	3	4	3	3	543
		5	8	8	7	459
Si–Cr (2) (0·50% Si)	S.T.A. 2*d*	7·5	7	8	7·5	540
		9	9	9	9	457
Cr–Mo	S.T.A. 12	3	2·5	2	2·5	555
		12	12	13	12	451
Ni–Cr–Mo	B.S. En 25	12·5	14	13·5	13·5	557
		10	9	9	9	451

in the region of 300–400° C., and as a tempering temperature of 400° C. was used to obtain a hardness value of 450 $H_D/30$ in the samples under discussion, this low izod value was not unexpected. The other two steels which were markedly affected by tempering to give the lower hardness of 450 $H_D/30$ were Cr–V and Cr–Mo steels, in which the impact values increased from 5 and 2·5 ft.lb. to 14 and 12 ft.lb. respectively.

Torsion Impact Tests

As heat-treated heavy coil springs are often subjected to rapid and violent loading in service, it was thought that torsion impact tests might reveal some interesting features. Consequently, a series of specimens was prepared and heat-treated to give the same hardness values (550 and 450 $H_D/30$) as in the tests recorded previously.

The results obtained are given in Table VII. As with the izod impact test, considerable differences between the steels were revealed by this test.

Of the specimens heat-treated to give a hardness value of 550 $H_D/30$, the Ni–Cr–Mo steel required the highest energy to produce fracture, and also showed the greatest deformation as measured by the angle of twist before fracture. Following in order were the Si–Cr samples (1·50% Si), Si–Mn, and the carbon steel. In comparison, the Cr–Mo, Si–Cr, and Cr–V steels had relatively low torsion-impact values and small angles of twist before fracture.

With the samples tempered at a higher temperature to give a hardness value of 450 $H_D/30$, the order of maximum energy absorption was somewhat altered in that the plain carbon steel had the highest torsion impact value, closely followed by the Si–Cr (1·50% Si) and the Si–Mn materials. As far as the remaining steels were concerned, the higher tempering temperature resulted in a considerable increase in the torsion impact value of the Cr–Mo steel, a decrease of about 50% in the value given by the Ni–Cr–Mo steel, and a fair increase in the value for the Cr–V steel which, however, had next to the lowest torsion impact figure of all the compositions tested.

It is of interest to note that, with the exception of the Ni–Cr–Mo steel, those steels which possessed the highest torsion impact strengths had the lowest izod impact values, and *vice versa*, for any one given hardness level.

Fatigue Tests

Fatigue tests were made on rotating cantilever specimens running at 3000 r.p.m. in an Avery-Schenck duplex machine, using an endurance basis of 5×10^6 reversals. It was realized that the normal basis for endurance tests is 10×10^6 reversals, but for various reasons it was not possible to adopt this standard for the present series of tests. The effect of the use of a limit of 5×10^6 reversals was, in any case, probably not very marked, as Hankins and Ford[8] observed that of 37 bending-fatigue-limit determinations on a basis of 10×10^6 reversals, similar results would have been obtained in 25 cases if the tests had been carried out on a 1×10^6 basis.

Table VII

TORSION IMPACT TESTS ON HEAT-TREATED SPRING STEELS

Steel	Specification	Torsion Impact, ft.lb.			Angle of Twist, °			$H_D/30$
		1	2	Av.	1	2	Av.	
Carbon	S.T.A. 2*a*	109	86	98	87	60	74	560
		177	175	176	207	201	204	462
Si–Mn	S.T.A. 2*b*	101	103	102	79	80	79·5	557
		145	164	154	159	188	173	457
Cr–V	S.T.A. 2*c*	67	59	63	48	45	46·5	543
		76*	96	...	98*	109	...	446
Si–Cr (1) (0·85% Si)	S.T.A. 2*d*	83	68	76	65	50	57·5	545
		114	130	122	127	141	134	455
Si–Cr (2) (1·50% Si)	S.T.A. 2*d*	117	99	108	101	82	92	557
		163	180	172	196	216	206	450
Cr–Mo	S.T.A. 12	79	77	78	58	60	59	550
		104	119	111	107	130	118	455
Ni–Cr–Mo	B.S. En 25	127	183	155	117	177	147	547
		88	82	85	100	90	95	447

* Specimen undersize.

Table VIII

FATIGUE TESTS ON HEAT-TREATED STEELS

Steel	Specification	$H_D/30$	Fatigue Limit, tons/sq. in. Not decarburized	Fatigue Limit, tons/sq. in. Decarburized*	Efficiency
Carbon	S.T.A. 2*a*	550	±58·5	...†	...
		450	±49·5	...†	...
Si–Mn	S.T.A. 2*b*	550	±58·5	±32	0·55
		450	±49·2	±38	0·77
Cr–V	S.T.A. 2*c*	550	±55	±36	0·64
		450	±46·2	±37	0·78
Si–Cr (1) (0·85% Si)	S.T.A. 2*d*	550	±48·5	±34·5	0·71
		450	±41·5	±32	0·77
Si–Cr (2) (1·50% Si)	S.T.A. 2*d*	550	±46·5	±32·5	0·72
		450	±41·5	±37·5	0·90
Cr–Mo	S.T.A. 12	550	±49·5	±39	0·79
		450	±43·5	±34·4	0·78
Ni–Cr–Mo	B.S. En 25	550	±49·2	±43·5	0·88
		450	±39·7	±35·5	0·89

*Depth of decarburization approximately 0·005 in.
†All decarburized specimens cracked on hardening.

Furthermore, owing to shortage of steel, the usual number of fatigue specimens could not be made, so that the procedure outlined below had to be adopted.

Two specimens only of each steel at each hardness level (450 and 550 $H_D/30$) were made, and a test on any one specimen was begun at a stress well below the anticipated fatigue limit. The specimen was then run for 5×10^6 reversals of stress, and the stress then increased by 2 tons/sq. in., followed by 5×10^6 reversals at the increased stress, and so on until the specimen fractured. The second test piece of the same steel at the same hardness was then tested using an initial stress just below that at which the previous one had broken, and this stress was raised by small increments, using 5×10^6 reversals at each load, until fracture occurred.

The results obtained, though not so accurate as those obtained in the normal manner, should at least be comparative and indicative of the differences in behaviour between the various types of steel. All specimens, before testing, were given a surface finish of 2 μ-in. on the active length.

Table VIII summarizes the results obtained. The fatigue properties of the various steels (not decarburized) tempered to similar hardness values, differ appreciably. With a hardness of 450 or 550 $H_D/30$, the maximum strength was shown by the plain carbon and Si–Mn materials, both of which behaved similarly when not decarburized. A slightly lower value was given by the Cr–V steel, while the values obtained for the two Si–Cr steels, and the Cr–Mo and Ni–Cr–Mo steels were considerably lower.

At a hardness of 450 $H_D/30$, the minimum value for the whole series was given by the Ni–Cr–Mo steel, which, as has been mentioned previously, was tempered at 400° C., a tempering temperature which increases the notch sensitivity of the material. This may account for the low endurance value shown by this sample.

Concerning the higher fatigue strength of the undecarburized steels at a hardness of 550 $H_D/30$, it is of interest to note that service tests of some springs made from Si–Cr steel to specification S.T.A. 2*d*, which were only lightly tempered to give a hardness in excess of 600 $H_D/30$, showed that such springs, when free from surface decarburization, had an extremely long life, whilst some heavy Si–Mn springs, stress-relieved at 250° C. after oil-hardening, and not given any further tempering treatment, behaved very well in fatigue tests. These results seem to show that where a decarburized surface layer can be avoided, an increase in hardness would result in a longer spring life, provided also that the surface finish of the spring is good, in order to counteract the increased notch sensitivity of the harder materials.

The presence of a decarburized skin approximately 0·005 in. deep had a markedly injurious effect on the fatigue properties of all the materials tested. In the absence of corresponding results for the plain carbon steel, the greatest effect of the decarburization is shown by the Si–Mn and Cr–V steels, which at a hardness of 550 $H_D/30$ have respective efficiencies of only 0·55 and 0·64. The steels with lower fatigue strengths when in the undecarburized condition were not so badly affected by surface decarburization, and showed efficiencies ranging from 0·71 to 0·88 at the higher hardness level of 550 $H_D/30$.

Tempering the steels to a lower hardness (450 $H_D/30$) minimized the effect of decarburization considerably, all the steels tested giving efficiency values of between 0·77 and 0·90. Similar results have been reported by other workers on the effect of decarburization on the fatigue strength of spring steels.[9, 10]

CONCLUSIONS

In addition to the hardenability tests recorded in Part I of this paper, a series of end-quench tests at temperatures ranging from 800° to 1050° C. has provided confirmatory evidence of the results obtained by total oil-quenching, namely :

(1) Of the steels tested, the nickel–chromium–molybdenum, and chromium–molybdenum varieties possessed the greatest hardenability. The hardenability was almost unaffected (for a 1·15-in. dia. bar) by variations in quenching temperature between 850° and 1050° C. A similar remark applies to the samples of silicon–manganese, and silicon–chromium steel used in the experiments.

(2) The carbon and chromium–vanadium steels could not be hardened so easily as the other steels used, though their hardenability was improved by raising the quenching temperature to 950° C. This increase in hardenability in the carbon steel cannot, however, be utilized in practice because of the susceptibility of this steel to quench-cracking at temperatures exceeding about 850° C.

The results of the determination of the mechanical properties and the fatigue strengths of the various materials, after oil-hardening and tempering to give hardness values in the region of 450 and 550 $H_D/30$, may be summarized as follows :

(i) Tensile tests revealed little difference in yield point or ultimate strength between the various compositions. The major differences were shown in the values for the reduction in area at fracture,

which varied, in the case of specimens having a hardness of 550 $H_D/30$, from a minimum of 24% for the Si–Cr (0·85% Si) steel to a maximum of 47% for the Ni–Cr–Mo material. The most commonly used spring steel, *i.e.*, Si–Mn, gave an intermediate value of 31%. Results were not obtained on two steels (carbon and Cr–Mo) which broke in the grips (presumably owing to their high notch sensitivity) before the yield point had been reached.

With specimens tempered to give a hardness of 450 $H_D/30$, the carbon steel had the lowest ductility whilst the Cr–V and Ni–Cr–Mo steels were the best in this respect.

(ii) Shear tests showed no important differences between the steels at either hardness level.

(iii) Izod impact tests showed considerable differences between the various steels, the carbon steel, in particular, giving very low values after oil-hardening and tempering to give hardnesses of 550 and 450 $H_D/30$.

With specimens heat-treated to give a hardness of 550 $H_D/30$, the izod impact values were low, and ranged from 2·5 ft.lb. (Cr–Mo) to 7·5 ft.lb. (Si–Cr), with the exception of the Ni–Cr–Mo steel which gave a value of 13·5 ft.lb.

Tempering to give a hardness of 450 $H_D/30$ resulted in considerable increases in the izod impact values of both the Cr–V and Cr–Mo materials, while similar treatment had little effect on the value given by the Si–Mn steel, which remained at the low figure of 4 ft.lb. With the Ni–Cr–Mo steel, the energy of fracture fell from 13·5 ft. lb. at 550 $H_D/30$ to 9 ft.lb. at 450 $H_D/30$, a decrease which was not unexpected in view of the facts that the izod-impact/tempering-temperature curve for this steel shows a marked dip in the region of 300–400° C., and that 400° C. was the tempering temperature used to obtain a hardness of 450.

(iv) As with the izod impact test, the torsion impact test revealed considerable differences between the various steels, though their significance is not understood. It is notable however, that, with the exception of the Ni–Cr–Mo steel, those steels which possessed the highest torsion impact values had the lowest izod impact values, and *vice versa*, for any one hardness level.

Of the series tested, the Cr–V steel had generally the lowest torsion impact values both at 450 and 550 $H_D/30$, whilst at the latter hardness, the Ni–Cr–Mo steel required the most energy to fracture. On the other hand, at a hardness of 450 $H_D/30$ the plain carbon steel gave the best results from the torsion impact test.

(v) Determination of the intrinsic fatigue strength of the various steels oil-hardened and tempered to give a hardness of 550 $H_D/30$ has shown that, when decarburization was absent, plain carbon and Si–Mn steels had the highest fatigue strength ($\pm$ 58·5 tons/sq. in.) closely followed by the Cr–V material ($\pm$ 55 tons/sq. in.) The values given by the other steels were considerably lower and ranged from $\pm$ 46·5 to $\pm$ 49·5 tons/sq. in. A similar order was shown at a hardness of 450 $H_D/30$, the carbon steel having a fatigue stress of $\pm$ 49·5 tons/sq. in. followed by the Si–Mn steel ($\pm$ 49·2 tons/sq. in. and the Cr–V steel ($\pm$ 46·2 tons/sq. in.) The other materials of the series gave values from $\pm$ 39·7 tons/sq. in. to $\pm$ 43·5 tons/sq. in.

The effect of the introduction of a decarburized skin 0·005 in. deep was most marked in the case of the Si–Mn steel and resulted in a decrease of fatigue strength from $\pm$ 58·5 tons/ sq. in. to $\pm$ 32 tons/sq. in. at a hardness of 550 $H_D/30$. The next most affected was the Cr–V steel, the fatigue value of which dropped after decarburization to 64% of its previous (undecarburized) value. The effect on the other steels, which had a lower intrinsic fatigue limit, was not so marked, the fatigue strengths in these cases dropping to a value between 71 and 88% of the original.

In general, similar results were given by the specimens heat-treated to a hardness of 450 $H_D/30$, though the effects of a decarburized skin were not so serious as they were at the higher hardness level.

In conclusion, it would appear that normal mechanical tests, alone, are of little use in assessing the effects of variation of composition, heat-treatment, etc., on the properties of spring steels, since important factors such as surface finish and decarburization, which had little or no effect on the results given by these tests, are known to have a profound influence in service.

Fatigue tests, however, emphasize the deleterious effects of surface decarburization, which should be avoided if a spring is to perform satisfactorily without premature failure during service.

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HISTORICAL NOTE No. 17

The Economic Aspect of Sir Henry Sidney's Steelworks at Robertsbridge, in Sussex, and Boxhurst, in Kent

By Dr. H. R. Schubert

In the Middle Ages, steel was imported into England in considerable quantities,[1] mainly by merchants of the German Hanseatic League, whose London office and warehouse was the Steelyard, at the site of the present Cannon Street Station.

Efforts at creating a home steel industry, however, have not been wanting in England. The first attempt known from documentary evidence, was the erection of a steel forge in 1509 by Claudius Robynson, in Ashdown Forest, near Hartfield, in Sussex.[2] It was worked by water power taken from a rivulet in the vicinity, and is mentioned last in 1555.[3] The capacity of steel production was estimated in 1539 at 40 to 50 tons per annum.[4]

The next known attempt was made by Sir Henry Sidney, who erected a furnace for the production of raw steel in Glamorganshire, and also steel forges at the former Cistercian Abbey of Robertsbridge, in Sussex, and at Boxhurst, in the parish of Sandhurst, in Kent, in 1565. Rhys Jenkins has elucidated the history of these works.[5] There is, however, new evidence in respect of the early beginnings of Sir Henry's undertakings, and also, from sources in foreign archives, an explanation of the causes of their failure after a short success.

On 8th August, 1565, Queen Elizabeth granted by Letter Patent, a 'licence' or privilege to Sir Henry Sidney, Edmund Roberts of Hawkhurst in Kent, merchant, Raufe Knight, and David Willard to introduce into the Realm of England, Ireland, and Wales, during twenty years following, "so manye strangers or alyens," as the patentees "shall entertayne onelie for the searchinge, digginge and conveyinge of the mynes ewer (*i.e.*, mine ore) and stone for the makinge of steele and iron wyer." The number of foreign workers was limited to one hundred.[6]

It is doubtful whether Sir Henry, the first of the four partners mentioned in the licence, took more than a capitalistic interest in the enterprise, as his duties as Lord Deputy of Ireland and President of Wales, kept him away from Kent and Sussex for the major part of his life. His wife, however, Lady Mary, daughter of John Dudley, Duke of Northumberland, was distinctly interested in metallurgy, as is demonstrated by her active interest in William Medley's attempt to convert iron into copper by heating it with blue vitriol.[7] The partnership of Edmund Roberts was most promising in respect of future prosperity, on account of his position in the iron trade. For many years before the erection of these steelworks he had been purchaser of the iron produced at Sir Henry's ironworks erected at Robertsbridge in 1541.[8] Roberts also was a member of the Merchant Adventurers, the great commercial company who, in sixteenth-century England, sent their ships to European ports, from the Mediterranean to the Baltic.[9] Knight and Willard were partners for only a short time. David Willard, first mentioned as an "yronmaker" of Tonbridge, in Kent, in 1555,[10] must have withdrawn from the partnership at a very early date, for he was never mentioned as a partner in subsequent documents concerning the steelworks. Raufe Knight, General Receiver of Sir Henry's English estates, died in 1567, and was succeeded in the partnership by his wife Johane.[11] Consequently from 1568 Sir Henry Sidney and Johane Knight were possessed of one half of the steelworks, and Edmund Roberts owned the other half.[12]

The next tasks of the partners were: (1) Obtaining a monopoly for the sole production of steel in England, and (2) procuring foreign workers according to the fore-mentioned privilege of 1565. In respect of the first task they were completely unsuccessful. Two Bills submitted to the House of Commons, one for "making of steel and plates for armour," the other for "making of steel and iron wire," were

[1] L. F. Salzman, "English Trade in the Middle Ages," p. 363. Oxford, 1931.

[2] Public Record Office, Ministers' Accounts of the Duchy of Lancaster, Bundle 454, No. 7331.

[3] *Ibid.*, Bundle 447, No. 7185.

[4] Forest Proceedings, Public Record Office, E32, No. 197.

[5] Rhys Jenkins, *Transactions of the Newcomen Society*, vol. III, pp. 16–40, London 1924, based on the accounts preserved at Penshurst Castle in Kent, abstracts of which are published in "Report of the Historical Manuscript Commission on the Manuscripts of Lord de l'Isle and Dudley," vol. I, p. 316 *et seq.* London, 1925. (Subsequently the Penshurst accounts will be quoted by their numbers only.)

[6] The licence is inserted in a Letter Patent of 7th July, 1568, Public Record Office, Patent Rolls, Elizabeth, Anno X (C 66 No. 1052).

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[7] John Strype, "The Life of the Learned Sir Thomas Smith." p. 105, new edition. London, 1820.

[8] Nos. 151 and 152; E. Straker, "Wealden Iron," p. 310. London, 1931.

[9] "Calendar of the Patent Rolls, Philip and Mary," vol. II, p. 56. London, 1936.

[10] *Ibid.*, p. 94. In 1574, Willard worked furnaces and forges near Tonbridge. E. Straker, *loc. cit.*, p. 222.

[11] Nos. 152, 154, 385.

[12] No. 385.

dealt with in 1566, but neither of them passed the House of Lords.[13] A monopoly excluding competition, aimed at by the two Bills, was not only refused to Sir Henry and his partners, but was granted, in respect of the manufacture of wire, to William Humfrey, who set up the wire works at Tintern Abbey, in Monmouthshire, which are well known in English economic history.

The preference given to Humfrey has a political background. Humfrey was in the closest contact with the leading statesman of the Realm, the Chancellor and Secretary of State, William Cecil, Lord Burghley. From the lengthy correspondence between the two, preserved in the State Papers Domestic of the Public Record Office, it is quite evident that Humfrey was Lord Burghley's adviser in technical and metallurgical matters. On the other hand, Sidney, by marriage with Mary Dudley, was brother-in-law to Robert Dudley, Earl of Leicester, who was the favourite of Queen Elizabeth for thirty years. The rivalry between the powerful statesman and the favoured courtier naturally extended to their adherents. Burghley never ceased to regard Henry Sidney with aversion and distrust. He even feared that Leicester might endeavour to have Sidney made Secretary of State in place of Burghley.[14]

When the manufacture of wire, which had been the principal object of the new enterprise, had to be abandoned in favour of a more successful competitor, production was limited to the manufacture of steel. For this purpose, large numbers of foreign steelmakers were engaged. Between 9th October, 1565 and May, 1566, they came to England in nine different groups, numbering altogether at least 55 steelmakers, some accompanied by their families.[15] The route taken by them indicates the country from which they came, which, in the accounts is called the "high country," from where they travelled to England *via* Cologne. The term "high country" points to the mountainous area of Southern Westphalia, containing the Sauerland, under the rule of the Archbishops of Cologne, and north of it, the Mark, ruled by the Dukes of Cleves and Mark. This assumption is confirmed by the family names of the steelmakers[16] which occur frequently in contemporary documents in the State Archive at Münster, in Westphalia, concerning Sauerland and Mark.

Both districts were the home of an ancient steel and wire industry. In respect of trade with England two towns were standing in the fore, Attendorn and Breckerfeld. Citizens of Attendorn are mentioned as pursuing their trade in England as early as 1328.[17] The high proportion of exports of German steel from Breckerfeld to England is reflected in a popular belief, prevailing in sixteenth-century Germany, that the Steelyard in London had its name from Breckerfeld steel exported into medieval England.[18] This is definitely a patriotic exaggeration, as the prosperity of the steel industry of Breckerfeld and its surroundings commenced only about 1415; but steel export from Breckerfeld became prominent in the course of the fifteenth century.[19]

The manufacture of steel at Robertsbridge and Boxhurst began early in December 1565; the wholesale trade began on the 20th of the following March, and extended to London, Coventry, Ipswich, York, Ireland, and Wales.[20] The effect in the English steel market was felt immediately by the German steel traders. Moritz Zimmermann, Alderman of the Steelyard in London, reported, in a letter dated the 9th August, 1567,[21] to Dr. Sudermann, Syndicus of the Hanseatic League in Cologne, on the status of the English steel market. There is no prospect, he said, of the contract concerning import of steel from Breckerfeld to London being renewed after its expiry on 1st August. More than 300 barrels of imported steel are still unsold, and there is no chance of them being sold, because, according to information obtained, the English have found means of manufacturing steel as good ("so guett sthaell") as the steel from Breckerfeld. This, he continued, is a great shock to the steel merchants of Cologne, as the London ironmongers will not venture to buy more steel than can be immediately sold. In a final sentence he pointed out that imports from the East were higher than ever before which, he thought, was another cause of the deterioration of the steel market.

Zimmermann's letter signifies the great initial success of the steelworks at Robertsbridge and Boxhurst. It also reveals the cause of the complete failure that soon followed, *viz.*, the extensive import of steel from countries bordering on the Baltic sea, particularly from Sweden. These imports in the latter part of the sixteenth century were effected by the Merchant Adventurers, who gradually supplanted the merchants of the German Hanseatic League in the English steel market. A single enterprise such as Sidney's steelworks had no chance of competing with such powerful rivals. Steel, imported from abroad, this time by English merchants, entered the London market in large quantities, and prevented the steel produced at Robertsbridge and Boxhurst from being sold at reasonable economic prices. The result was a rapid fall of sales and prices, which is noticeable in the existing accounts. The annual sales of steel being 166·8 firkins in 1566 (sale price per firkin £7 in March, average price £6 10*s.* from March

[13] S. D'Ewes, "The Journals of All the Parliaments During the Reign of Queen Elizabeth," pp. 126, 132, 133. London, 1682.

[14] P. Sidney, "The Sidneys of Penshurst," p. 33. London, 1901.

[15] No. 384.

[16] Rhys Jenkins, *loc cit.*, List of names, p. 34.

[17] "sepius in eorum negotiationibus regnum Anglie frequentantes," Seibertz: "Urkundenbuch zur Landes und Rechtsgeschichte Westfalens," vol. 2, p. 235. Arnsberg, 1843.

[18] A. Meier, "Geschichte und Urkundenbuch des Amtes Breckerfeld im Landkreise Hagen (Westfalen)," p. 128. Hagen, 1908.

[19] E. Dösseler, "Westfalen und die See," pp. 43, 113, 118. Thesis of Berlin University, 1932.

[20] No. 384.

[21] The letter is preserved in the Historical Archives of the City of Cologne, printed by A. Meier: *loc. cit.*, p. 224.

[22] Nos. 384, 385, and 147 (for 1570–71); *cp.* Rhys Jenkins *loc. cit.*, pp. 35–36. The weight of the firkin cannot be ascertained; Jenkins (p. 35), assumed "about 6 cwt.," but it seems to be more. In Accounts 1567–68 (No. 385) it is stated that in 4 months, 57 firkins were made, but only a little more than 17 were sold.

to August) fell to 51 firkins in 1568 (sale price £6), and to 18½ firkins in 1570–71 (sale price £5).[22]

Production appears to have ceased shortly after 1572, when there was still a proper clerk appointed to the steelworks[23]; there is no reference to later production in any subsequent account. In the same year, 1572, a payment to Edmund Roberts on account of his partnership is mentioned for the last time. Later, Edmund Roberts had a gun foundry at Abercarne in Monmouthshire, which he possessed at the time of his death, about 1581. He died "a poor man" leaving debts of £2700, "at the least," which was a considerable sum in those days.[24]

Despite the very short period of prosperity, the two steelworks at Robertsbridge and Boxhurst are remarkable as an early attempt to make England independent of steel imported from abroad.

[23] No. 148. The Robertsbridge works is mentioned in a survey of the Manor dated 1609, preserved only in a transcription of 1783 (British Museum Add. MS. 5680, fol. 91). Jenkins (p. 18) doubts whether the establishment was still in operation in 1609; perhaps the description refers to an earlier period when the German steelworkers were still working.

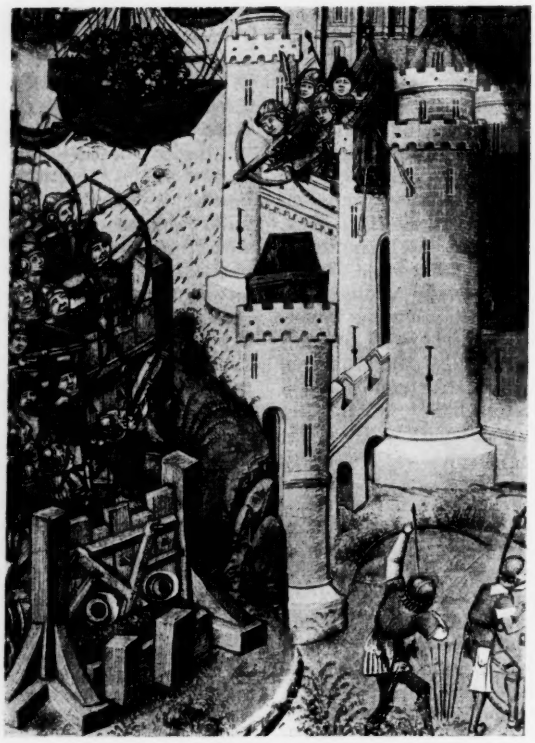
[24] E. G. Jones, "Exchequer Proceedings (Equity) Concerning Wales." Board of Celtic Studies, University of Wales, History and Law Series, No. IV, p. 257. Cardiff, 1939.

Historical Note No. 18

The First Iron Handguns in England

By Dr. H. R. Schubert

Small firearms called handguns, the forerunners of modern rifles and pistols, were first used in England in the reign of Richard II (1377–1399). The term "handgun" was applied for the first time in 1386, when Ranulph de Hatton, Keeper of the King's Privy Wardrobe, delivered to John of Derby, chamberlain of the castle and town of Berwick-on-Tweed, "three small cannon of brass, called handgonnes."[1] Later, when Robert Clavering was succeeded by John of Lancaster as chamberlain of Berwick-on-Tweed, an inventory of all "arms, artillery and stores" was made, dated 22nd September, 1389.[2] In the inventory some handguns are mentioned, all of them fixed on wooden staffs supporting their weight. Three of these handguns were of iron (*gunnas ferreas . . . vocatas handgunes*). This is the first evidence of handguns made of wrought iron in the history of England.



The shape of such handguns is illustrated by a picture of the siege of Mortagne, a fortress on the mouth of the river Garonne in the south of France, held by the English and attacked by a French army commanded by Ivain de Galles.

In this picture[3] it is significant that the French were armed with handguns; and not the English, who defended themselves with crossbows and long bows. This is also demonstrated by numerous other pictures of late medieval battles. It shows that the bow remained the national weapon of the English to the end of the Middle Ages. Handguns, especially those of iron, such as the three handguns mentioned in the inventory of 1389, were still weapons of great rarity in England.

[1] T. F. Tout, "Firearms in England in the Fourteenth Century," English Historical Review, vol. XXVI, pp. 684 and 699. London 1911.

[2] Public Record Office, Exchequer Various Accounts, Bdle. 41, No. 13B.

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[3] Chroniques d'Angleterre by Jean de Wavrin, vol. III. British Museum. Royal MS. 14 EIV fol. 23 (vol. III has not been published; it deals with the reign of Richard II until 1387).

Treatment of Waste Waters from the Pickling of Steel

By G. E. Eden, B.Sc., A.R.I.C., and G. A. Truesdale

SYNOPSIS

Experiments of Gehm on the neutralization of dilute solutions of sulphuric acid by application in upward flow to a bed of limestone grit have been extended to show that the limiting concentration of acid that can be satisfactorily neutralized in this way is dependent on the temperature of the solution. The concentration of sulphuric acid (as H_2SO_4) which could be continuously neutralized without causing inactivation of the particles of limestone ranged from 9880 p.p.m. at 6° C., to 4360 p.p.m. at 30·4° C., and at any given temperature appeared to correspond approximately to the concentration of sulphate in a solution of calcium sulphate saturated with respect to the metastable calcium sulphate hemihydrate.

When the method was applied on a laboratory scale to neutralization of solutions similar in composition to waste washing waters from sulphuric pickling of steel, iron in the form of ferrous sulphate did not interfere with neutralization of free acid, and was not precipitated in the bed. Although iron in the form of ferric sulphate did not interfere with neutralization of free acid, hydrolysis of residual ferric sulphate caused an increase in the titratable acidity of effluent from the bed ; some precipitation of ferric iron was brought about by the action of the limestone.

The process of application at a high rate of upward flow to a bed of small particles of limestone might be of value in reducing the acidity of waste waters from the pickling of steel, and would have the advantage that large volumes of waste water could be treated in a comparatively small plant.

LARGE volumes of spent pickle liquor, containing acid and dissolved iron, and very large volumes of waste washing waters containing lower concentrations of the same substances, are produced in the pickling of steel. Attention has been directed to the development of methods for the treatment of spent pickle liquors,[1] with the object of recovering acid for re-use in the pickling process. In Great Britain, diluted sulphuric acid is by far the most widely used reagent for the pickling of steel. In one process of recovery that has been successfully used on a commercial scale, spent pickle liquor of this type is cooled to bring about crystallization of ferrous sulphate, and the strength of the liquor is restored by addition of fresh acid before or after separation of the ferrous sulphate. Although the possibility of recovering acid is attractive, it is difficult to find a suitable outlet for the large quantities of ferrous sulphate formed as a by-product, and at most mills, pickle liquor is not regenerated, but is run to waste, usually after dilution with waste washing water.

Waste washing waters, even when not mixed with spent pickle liquor, contain sufficient acid and dissolved iron to render them unsuitable for discharge to a river or to a sewer, unless the dilution available is very large. If discharged untreated to a watercourse, pickling wastes may render the water acid, and the dissolved oxygen in the water may be seriously depleted in the oxidation of ferrous to ferric salts, which may later produce deposits of ferric hydroxides or basic ferric sulphates. If allowed to flow into a sewer, free acid may attack the materials of construction of the sewer, and salts of iron may interfere with treatment of the sewage at a sewage-disposal works. In most circumstances, therefore, it is desirable to neutralize free acid, and sometimes it may also be necessary to oxidize or precipitate dissolved iron.

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NEUTRALIZATION OF ACID WASTE WATERS WITH LIMESTONE GRIT

Because of its low cost and relative abundance, limestone has often been used as a neutralizing agent. In one method, acidic waste water is allowed to percolate through a tank filled with lumps of limestone. At first, neutralization may be satisfactory, but after a short time the amount of neutralization taking place in the tank generally becomes negligible even though most of the limestone is still unused. The main reason for the fall in efficiency is probably that calcium sulphate is deposited on the stone and covers it with an inactive layer. In a treatment plant of this type the rate of flow of waste water is usually relatively low, and although calcium sulphate tends to form supersaturated solutions, there is probably time for equilibrium to be reached between calcium sulphate in solution and the stable solid phase of calcium sulphate dihydrate, whose solubility is 2130 parts of $CaSO_4$ per million at 25° C. Thus, if complete neutralization of the waste water gives a concentration of calcium sulphate in excess of this value, deposition of the solid salt is liable to occur. If ferrous sulphate is present in the waste waters, precipitation of hydroxides of iron may also contribute to the inactivation of the limestone.

To overcome these difficulties, a method has recently been described by Gehm[2] and by Reidl[3] in which the acidic solution is forced upward, at a considerable velocity, through a bed of fine limestone grit. The size of the particles and the upward rate of flow are such that the bed of limestone is 'expanded,' *i.e.*, the particles are lifted by the stream of liquid so that they are not in permanent contact and are constantly in motion. The action is similar to that which takes place during the backwashing of a sand filter. In Gehm's laboratory-scale apparatus the particles had a diameter of 1 to 2·5 mm., and the rate of flow was from 18 to 50 gal.*/min./sq. ft. Under these conditions

* Throughout this paper volumes are given in Imperial gallons.

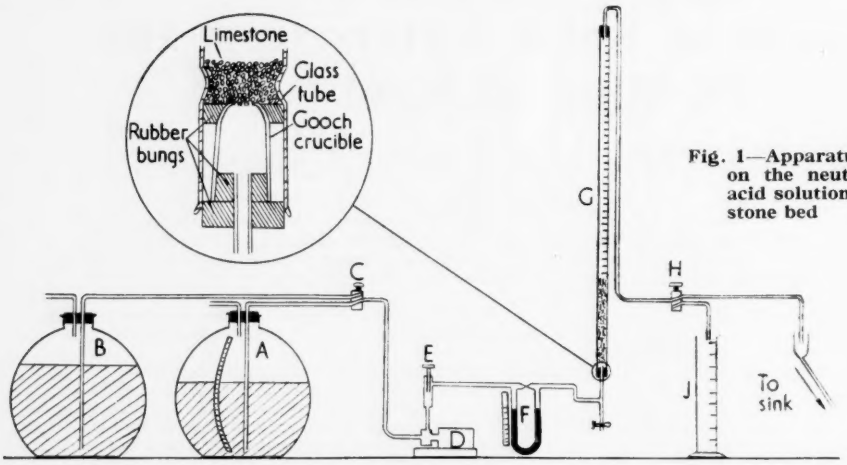

Fig. 1—Apparatus used for experiments on the neutralization of sulphuric acid solutions in an expanded limestone bed

a bed with a resting depth of 12 in. would be expanded by 50 to 100% of its depth. By operating the bed in this way, all the products of the reaction are swept out almost as soon as they are formed, and, provided that the acidity of the influent is not too great, all the limestone will eventually be used.

In a typical experiment of Gehm a sulphuric acid solution of 2000 p.p.m. was passed through a bed with a resting depth of 12 in. at a rate of 47 gal./min./sq. ft. and gave an effluent with a *p*H value of 4·2. This effluent was saturated with carbon dioxide, removal of which by agitation or aeration would raise the *p*H value considerably.

It has been reported[3] that a full-scale plant operating on this principle was built at the factory of the General Electric Company, Philadelphia, for the neutralization of acid dipping and plating wastes. The settled acidic waste water, mixed with twice its volume of recirculated effluent, is passed upwards through two beds (each 2 ft. deep) in series, at a rate of 33 to 42 gal./min./sq. ft. The *p*H value of the waste water is 1·9 to 4·4, and the *p*H of the treated waste water is 5·0. The *p*H value is increased to 6·0 by circulating part of the effluent in the final storage tank through sprays.

Gehm found that use of the expanded limestone bed for neutralization of sulphuric acid still had certain disadvantages. If a sulphuric acid solution of more than 5000 p.p.m. was passed through the bed, neutralization proceeded for a short time, after which there was a rapid fall in activity of the limestone, presumably owing to deposition of calcium sulphate. The activity of the bed could not be restored by washing with dilute solutions of other mineral acids. Gehm also reported that when pickle liquor, diluted to contain 2400 p.p.m. sulphuric acid, was passed through a limestone bed, complete removal of acid could not be achieved, even at low rates of flow.

TREATMENT OF DILUTE SULPHURIC ACID IN AN EXPANDED BED OF LIMESTONE GRIT

Laboratory experiments on neutralization of dilute sulphuric acid were made in an apparatus similar in principle to that used by Gehm (*see* Fig. 1). The calibrated carboy, *A*, contained the acid solution, which was standardized volumetrically, and a similar carboy, *B*, contained tap water used for priming the pump, *D*, and for washing out the apparatus at the end of an experiment. A two-way tap, *C*, was provided so that either acid or water could be pumped by way of a regulating valve, *E*, and a flow meter, *F*, to the base of the limestone column, which was composed of high-calcium limestone, graded − 10 to + 20 mesh, and supported on a perforated porcelain plate in a vertical calibrated glass tube, *G*, of 1-in. dia. In most experiments the bed had an initial resting depth of 12 in., but in a few experiments a 24-in. bed was used. By means of the two-way tap, *H*, effluent leaving the top of the column could be passed to waste or diverted to a sampling cylinder, *J*, as required.

In each experiment, after limestone dust had been washed from the column by tap water, the resting depth of the bed was adjusted to the required value and, again using tap water, the regulating valve was set to give a rate of flow of 0·9 to 1·0 litre/min. The acid solution was then admitted to the bed, and samples of effluent were taken for analysis at intervals of 2 to 4 min. Each time a sample was taken, the volume of liquid in carboy *A* and the expanded volume of the bed were noted. At the end of each experiment the limestone bed was allowed to settle, and the residual resting depth was noted after tapping down to constant level.

The results showed that the experiments could be divided into two groups, as judged by the following criteria :

(i) *Experiments throughout which the bed remained active*

(*a*) The expanded volume of the bed decreased steadily throughout the run

(*b*) Evolution of carbon dioxide continued throughout the run

(*c*) The effluent remained clear for several minutes after leaving the bed, and then slowly deposited coarse needle crystals, presumably of $CaSO_4.2H_2O$

(*d*) The bed continued to neutralize acid throughout the run so far as its reduced volume (in the later stages) would allow.

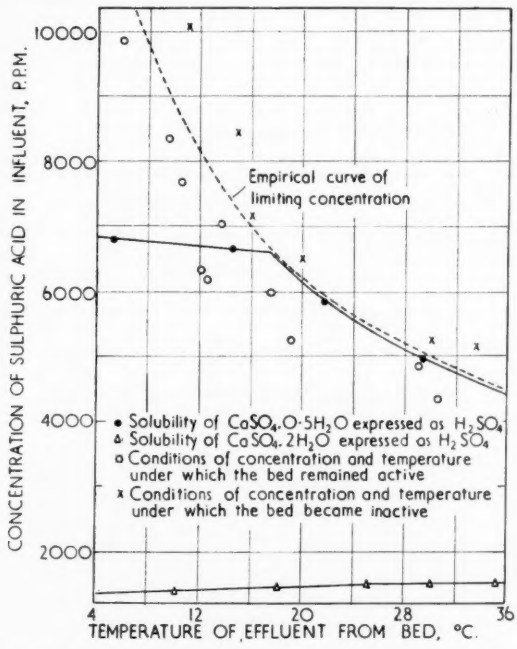

Fig. 2—Effect of temperature on the maximum concentration of sulphuric acid which can be continuously neutralized in an upward-flow expanded bed of limestone grit

(ii) *Experiments in which the bed became inactive*

(*a*) The volume of the bed decreased steadily for a few minutes, but then became constant

(*b*) Evolution of carbon dioxide ceased at about the time that the volume of the bed became constant

(*c*) The effluent at first rapidly deposited fine needle crystals which occupied nearly the whole volume of the solution; when the activity of the bed decreased, this precipitate was no longer produced

(*d*) The bed neutralized acid satisfactorily at first and then rapidly became inactive; the acidity of the effluent then rose to a value approaching that of the influent.

The results of a series of experiments, made at different temperatures and using different concentrations of sulphuric acid, are summarized in Fig. 2. The points plotted indicate whether the bed remained active or not, and an empirical curve has been drawn between the two sets of points to indicate approximately the maximum concentration of sulphuric acid at which the bed of limestone would become inactive at any given temperature. The maximum concentration of sulphuric acid which could be satisfactorily neutralized increased rapidly as the temperature fell. Above 20° C. the curve of limiting concentration follows fairly closely the solubility curve of calcium sulphate hemihydrate drawn from data quoted by Seidell.[4] Although there is considerable divergence below 20° C., it has been pointed out by Seidell that the accuracy of determinations of the solubility of calcium sulphate hemihydrate at low temperatures is open to doubt. These observations indicate that under the experimental conditions used, the concentration of sulphuric acid that can be neutralized is limited by the solubility of the metastable hemihydrate at any given temperature. A possible explanation may be that conditions in the upward-flow bed are unfavourable for formation of the stable dihydrate, and that no change occurs in the system until the solution formed by neutralization becomes supersaturated with respect to the metastable calcium sulphate hemihydrate. Once deposited, the hemihydrate would no doubt change to the stable dihydrate; as already mentioned, solutions which were not sufficiently concentrated to inactivate the bed did afterwards deposit calcium sulphate on standing. This behaviour is not peculiar to solutions of calcium sulphate, but is frequently observed in metastable systems, which tend to pass to the most stable form of the system by traversing intermediate conditions of progressively greater stability.[5]

Small-scale experiments showed that limestone grit which had been rendered inactive by contact with a sufficiently concentrated solution of sulphuric acid, could be reactivated by treatment with solutions of ammonium acetate, sodium thiosulphate, and sodium hexametaphosphate (Calgon), all of which are known to dissolve calcium sulphate. An inactive bed was also reactivated by the circulation through it for 5 min. of 1 litre of a 1% solution of Calgon. Attempts to prevent inactivation of a bed by addition of Calgon to the acidic liquid being treated met with very limited success, probably because of the relatively high concentration of calcium sulphate produced, which would require a correspondingly high concentration of Calgon to prevent precipitation.

EFFECT OF FERROUS AND FERRIC SULPHATES ON NEUTRALIZATION OF SULPHURIC ACID IN AN EXPANDED BED

Two solutions containing ferrous sulphate and sulphuric acid were prepared, the first by dissolving metallic iron in a dilute solution of the acid, and the second by mixing a decanted solution of commercial ferrous sulphate with diluted acid. The first solution was found to contain 3040 p.p.m. sulphuric acid, 8450 p.p.m. ferrous iron, and 30 p.p.m. ferric iron; and the second, 3050 p.p.m. sulphuric acid, 8400 p.p.m. ferrous iron, and 170 p.p.m. ferric iron. The content of acid was determined by titration with standard alkali, using screened methyl orange as indicator; as a solution of a ferric salt has an acid reaction to methyl orange, the content of free acid estimated in this way includes acid produced by hydrolysis of ferric sulphate.

Results of determination of free acid in effluents obtained by passing each of these solutions through a limestone bed of which the initial resting depth was 24 in. are compared, in Table I, with results obtained after similar treatment of a solution containing 3050 p.p.m. sulphuric acid, but no ferrous or ferric salts. In the solution containing a high concentration of ferrous iron and little ferric iron, neutralization was nearly as complete as in the solution of sulphuric acid alone; there was no evidence of precipitation of ferrous iron. In the solution containing a rather higher concentration of ferric iron, neutralization of acid was not quite so effective.

When a solution containing 320 p.p.m. ferric iron, but no ferrous iron, and having a titratable

Table I

EFFECT OF FERROUS SULPHATE ON NEUTRALIZATION OF SULPHURIC ACID IN AN EXPANDED BED OF LIMESTONE GRIT

Resting depth of limestone bed	24 in.
Diameter of limestone bed	1 in.
Grading of limestone grit	− 10 + 20 mesh (B.S. grading)
Temperature	20° C. (approx.)

Composition of Liquid before Treatment, p.p.m.			Rate of Flow of Liquid, litres/min.	Volume of Liquid passed through Bed, litres	Composition of Liquid after Treatment	
Titratable Acidity to Methyl Orange (as H_2SO_4)	Ferrous Iron (as Fe)	Ferric Iron (as Fe)			Titratable Acidity to Methyl Orange, p.p.m. H_2SO_4	*p*H Value
			1·02	2	−80	5·1
			1·03	7·5	−80	5·1
3050	0	0	1·0	11·5	−70	5·1
			1·0	17·5	−70	5·1
			1·0	25·5	−70	5·1
			1·0	4	−50	4·6
			1·02	14	−30	4·4
3040	8450	30	0·98	23	−20	4·4
			1·0	37	−10	4·4
			1·02	4	−150	3·8
			1·0	10	−170	3·8
3050	8400	170	1·03	15	−190	3·8
			1·02	21	−210	3·8
			1·02	31	−250	3·7

acidity (to methyl orange) equivalent to 2790 p.p.m. sulphuric acid, was passed through a 24-in. bed at a rate of 1 litre/min., some ferric hydroxide was precipitated, and the titratable acidity of the effluent was reduced to 340 p.p.m.; the *p*H value of the effluent was 3·4. If it is assumed that the ferric salt is completely hydrolysed during titration to methyl orange, the content of iron in the untreated liquid would be equivalent to 840 p.p.m. sulphuric acid. It appears, therefore, that in the presence of ferric iron, the whole of the free acid and some of the acidity caused by hydrolysis of the ferric salt were removed by the limestone bed.

DISCUSSION OF RESULTS

In circumstances where neutralization of free acid only is required, application of waste waters at a high rate of upward flow to a bed of limestone grit might be of considerable value for treating waste washing waters from sulphuric pickling of steel, because of the cheapness of the neutralizing agent and because a comparatively small plant could deal with a large volume of liquid. Thus, at the rate of flow used in laboratory experiments (approximately 40 gal./min./sq. ft.), about 900,000 gal. of waste water could be treated during 24 hr. in a bed of 16 sq. ft.; a depth of 1 ft. of limestone grit would probably be sufficient. As limestone is consumed during operation, it would be necessary to refill the bed periodically. To allow this to be done conveniently, it would be desirable to have two beds which would be in use alternately.

It might also be possible to use an upward-flow bed for neutralization of a mixture of waste washing waters and spent pickle liquor, if the proportion of spent pickle liquor added was controlled so as not to exceed the critical concentration of acid at which the limestone is rendered inactive.

Effluent from a limestone bed of this kind would still contain ferrous and ferric salts in solution and would be saturated with carbon dioxide. If carbon dioxide were removed by spraying or by agitation, the acidity of the effluent could be further reduced.

When removal of dissolved iron as well as neutralization of acid is required, the use of an upward-flow limestone bed, followed by completion of neutralization by addition of lime, might still be advantageous in securing economy in the amount of alkali required. A somewhat similar 'split' process using limestone flour and lime, has been suggested by Hoak, Lewis, and Hodge.[6]

Acknowledgments

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The Determination of the Texture of Rolled Sheet from X-Ray Diffraction Photographs

By A. E. De Barr, B.Sc., F.Inst.P., and B. Roberts, B.Sc.

SYNOPSIS

Full information on the texture of rolled sheet can be obtained from only three or four diffraction photographs if use is made of all the diffraction data recorded. Pole figures may be constructed if required. The method is illustrated with reference to the texture of rolled iron sheet, but may be applied to other materials. The advantages of the method over the usual procedure for the construction of pole figures are considered briefly.

Introduction

DATA on texture and preferred orientations are most conveniently represented by pole figures, as first described by Wever,[1] but although the pole figure gives, concisely, all the data available, it is the interpretation of the pole figure which is of most interest to the investigator, particularly if the texture is complex. Thus the stages in a texture determination[2] are :

(1) Taking a series of X-ray diffraction patterns, with the specimen inclined at different angles to the incident beam. Some 10 to 12 exposures are usually required

(2) Plotting the intensity maxima on each diffraction ring on to reflection circles on the pole figure, and drawing in the contours

(3) Interpreting the pole figure in terms of one or more ideal orientations and deviations from these.

The correct interpretation of complex figures often presents some difficulty, and it may be necessary to plot the corresponding figures for other poles to assist the interpretation.

Wood[3] has shown that the number of exposures required for a pole figure can be greatly reduced if all the data recorded on each photograph are used. Thus, if Mo or Ag Kα radiation is used, diffraction rings may easily be recorded from four or more sets of

Manuscript received 21st June, 1949. Mr. De Barr and Mr. Roberts are at the Guest, Keen and Nettlefolds Research Laboratories.

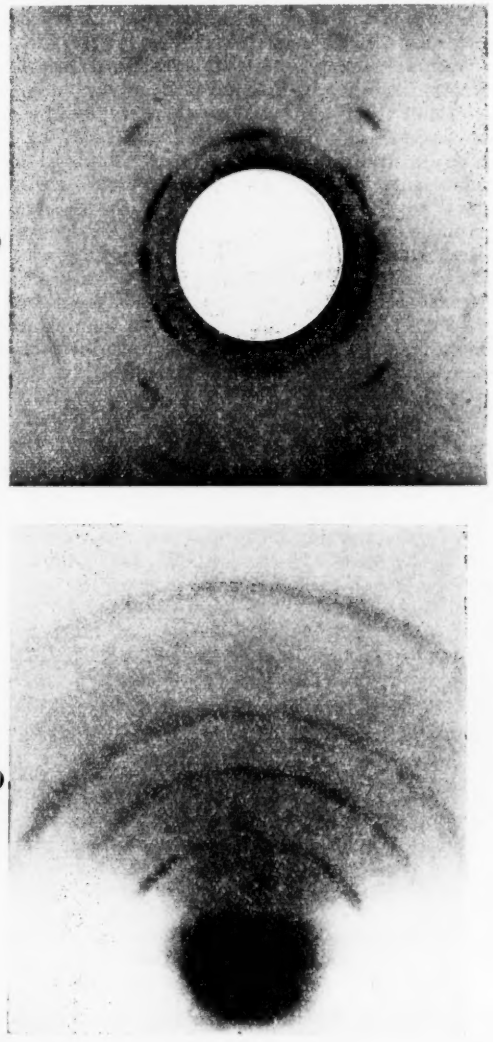


Fig. 1—X-ray diffraction photographs of rolled iron sheet (Mo Kα radiation) : (a) Incident beam normal to sheet surface ; (b) Incident beam 10° to sheet surface and parallel to rolling direction ; (c) Incident beam 10° to sheet surface and perpendicular to rolling direction.

The rolling direction is towards the top of the figure in all cases

planes, and all the data can be incorporated in the pole figure. To do this it is necessary to know, for example, where the {100} poles are when a (110) pole is in a reflecting position. Wood discusses this problem analytically and shows how the data may be plotted. His analysis is not rigorous, however, and can only be applied in simple cases, as it assumes that the final position of a (100) pole, when the crystal has been rotated from its ideal position to bring a (110) pole into a reflecting position, depends only on the final position of the (110) pole. In general, this is not true, as the final position depends also upon the nature of the rotation, and therefore the data cannot be plotted without some knowledge of the texture.

However, by making use of all the diffraction data in the manner to be described, it is possible, in most cases, to determine the texture of rolled sheet from three exposures. In cases of very complex texture, one additional exposure is all that is likely to be required to check a doubtful feature. The data are treated graphically, no analysis is required, and the texture is determined directly from the photographs ; a pole figure may be plotted if required.

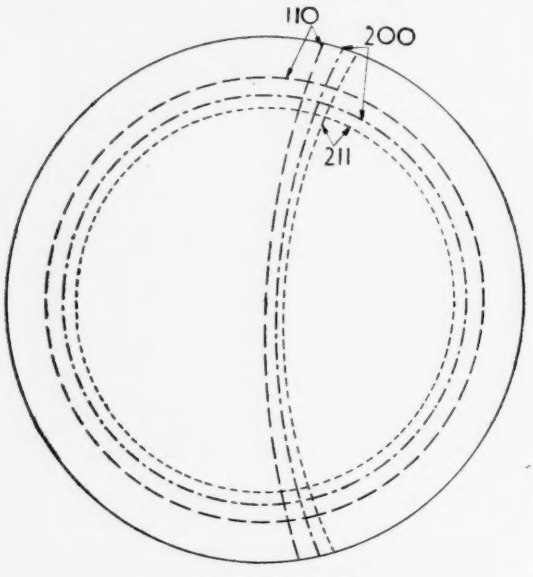
METHOD

The procedure will be described with reference to the texture of rolled iron, although it is applicable to other materials. For iron, Mo Kα radiation is the most convenient, and diffraction rings from {110} {200}, {211}, and {220} planes may be recorded in each exposure, as illustrated in Figs. 1*a*, *b*, and *c*, which represent a simple rolling texture. The ideal orientation, or orientations, on which the texture is based, is determined first, either from experience and inspection of the photographs or by trial and error. Deviations from this ideal are then postulated and their anticipated effect on the diffraction rings is considered and compared with the photographs, adjustments being made until complete agreement is reached on all the diffraction arcs recorded.

The effect of the various deviations from the ideal orientation may be examined very easily by using a stereographic projector,[4] an optical device for studying the effect of rotations on a stereographic projection ; or they may be examined graphically using a stereographic net and standard projections of {110}, {200}, and {211} poles for various ideal orientations. These are conveniently plotted, to the scale of the stereographic projection, on Perspex sheet, which can be superposed on the projection. A second Perspex sheet, engraved with the reflection circles for these planes for chosen angles of incidence, is also useful. Such a sheet is illustrated in Fig. 2.

THE DETERMINATION OF TEXTURE

The determination of texture will be described with reference to Fig. 1. If the information from the 110 and 200 rings on the transmission photograph (Fig. 1*a*) is transferred to the appropriate reflection circles, it will be seen that the sheet texture is, generally, (001) [110]. This ideal orientation is indicated in Fig. 3, and the nature and extent of the deviations from this ideal may be found by the following method. The 110 reflection circle for Fig. 1*a* lies 10° from the $(\bar{1}10)$ and $(1\bar{1}0)$ poles of the ideal orientation. Thus, to produce $(\bar{1}10)$ and $(1\bar{1}0)$ arcs at 90° to the rolling direction it is necessary to postulate crystals rotated $\pm$ 10° about the rolling direction as axis. On increasing this rotation to $\pm$ 20°, the (200), $(\bar{2}00)$, (020), and $(0\bar{2}0)$ poles appear on their reflection circle, 43° from the rolling direction and corresponding to the maxima on the diffraction photographs. A further rotation to a total of 45° in each direction allows the (011), $(0\bar{1}1)$, (101), and



$\theta_{110} = 10° 4'$, $\theta_{200} = 14° 18'$, $\theta_{211} = 17° 42'$

Fig. 2—Reflection circles for iron with Mo Kα radiation. Normal incidence and glancing (10°) incidence

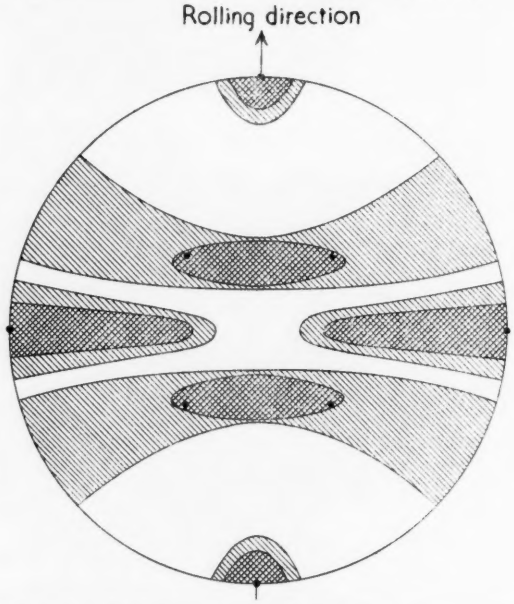


Fig. 3—110 pole figure for rolled iron sheet corresponding to the photographs of Fig. 1

($\bar{1}01$) poles, near to the centre of the projection, to reach reflecting positions and to produce the arcs 60° from the rolling direction. This amount of rotation is sufficient also to bring 211 poles into reflecting positions. Furthermore, the length of the ($1\bar{1}0$), ($\bar{1}10$), and (002) arcs on the diffraction photograph with the incident X-ray beam parallel to the rolling direction and making an angle of 10° with the sheet plane (Fig. 1*b*), is governed by rotation of the ideal orientation about the rolling direction. The rotation necessary to form these arcs is up to $\pm$ 60°. Thus, the rotation about the rolling direction extends from 0 to 60° in each direction. As the reflection circles in Fig. 1*b* lie nearly parallel to the transverse direction of the sheet, the distribution of intensity in the 110 and 200 arcs on this photograph is caused, almost entirely, by rotations from the ideal position about the rolling direction as axis. Thus, the distribution of the crystals about this axis may be obtained from the intensities of the 110 and 200 arcs in Fig. 1*b*.

The (110) and ($\bar{1}\bar{1}0$) arcs at 0° to the rolling direction, in Fig. 1*a*, indicate that some of the crystals are rotated $\pm$ 10° from the ideal position about the transverse direction as axis, and on the diffraction photograph with the beam at 90° to the rolling direction and making an angle of 10° with the sheet (Fig. 1*c*), the (112), ($\bar{1}\bar{1}2$), and (002) arcs also indicate a spread about the ideal position of up to $\pm$ 12°. The distribution of the crystals about this axis may be obtained from the intensity distribution in the (002) arc in Fig. 1*c*.

Similarly, it may be seen from the transmission photograph (Fig. 1*a*) that there is a rotation about the normal to the sheet surface. The extent of this rotation may be measured on the ($\bar{1}10$) and ($1\bar{1}0$) arcs, and is $\pm$ 7° for this specimen, although there are some crystals with up to twice this deviation. The distribution of these crystals is also given by the intensity distribution in the ($1\bar{1}0$) and ($\bar{1}10$) arcs.

The texture of the sheet is thus completely described in terms of one ideal orientation, (001) [110], and deviations from this ideal in the form of rotation about three axes, the distribution being known in each case. This information can be applied to give the complete pole figure for any desired poles. The resulting 110 pole figure is given in Fig. 3. From this it is easy to verify that the texture will actually give rise to the diffraction arcs observed. Although the nine reflection circles considered do not completely cover the whole area of the pole figure, the presence of any crystals not conforming to the general texture will, in almost all cases, be indicated by extra diffraction arcs on at least one of the photographs.

This example is, of course, a particularly simple one. The texture is based on a single orientation only, the deviations from the ideal are small, and consist of rotations about clearly defined axes. The method described is not limited to simple textures, however, and may be applied successfully to more complicated structures. Figure 4 shows a 110 pole figure for a partially recrystallized sample of rolled iron sheet in which there are three ideal orientations with large deviations from the ideal. Figure 4 also shows the construction lines, indicating how the complete pole figure was built up from the data

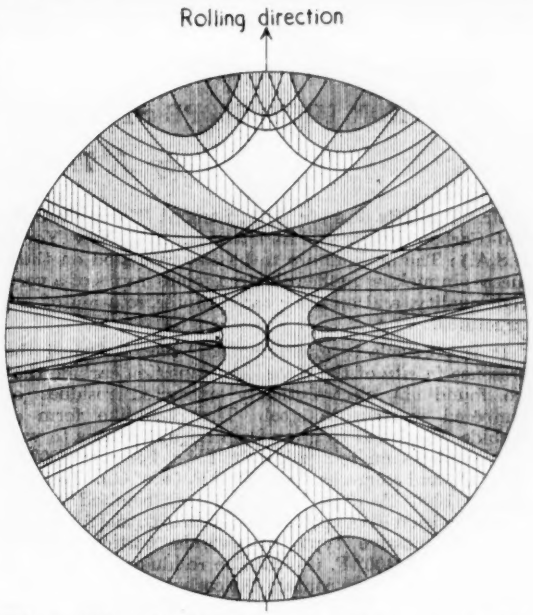


Fig. 4—110 pole figure for partially recrystallized rolled iron sheet

obtained by interpretation of the diffraction photographs. The method is of great value in cases of this nature, since the information obtained by interpretation of the diffraction photographs is, in general, far more reliable than a pole figure constructed in the usual way. This is partly because of the avoidance of errors in plotting data from several photographs, and partly because of the ease with which the relative intensities of different portions of the pole figure can be measured in this method. In constructing pole figures in the usual manner, it is often forgotten that the finished pole figure must conform to the crystal symmetry of the material studied, and many published pole figures fail in this respect. Not only must there be equivalent numbers of all poles of the type plotted, but their relative positions must be consistent with the symmetry of the crystal. It is also well known that quite small changes in relative intensities can alter the appearance of a pole figure very considerably, and similarly, small errors in intensity may lead to incorrect interpretation of the pole figure. Pole figures constructed in the manner described avoid errors of these kinds, since all the poles are considered together.

Acknowledgments

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Correspondence on the Paper—

ELECTRON MICROSCOPE STUDY OF QUENCHED AND TEMPERED STEEL*

by J. Trotter and D. McLean

Mr. **W. Crafts** and Mr. **J. L. Lamont** (Union Carbide and Carbon Research Laboratories, Inc., New York, U.S.A.): The authors are to be congratulated on their success in making replica electron micrographs of a very high quality, and on their careful interpretation of the structures.

It is particularly gratifying to have confirmed the presence *in situ* of the plate-like transition carbide that we found[1] in electrolytically extracted residues of tempered martensitic steel. The plate-like form of carbide was found to grow from nebulous films to well formed plates on tempering for 2 hr. between 400° F. (204° C.) and somewhat over 800° F. (426° C.). Tempering at 1000° F. (538° C.) and higher resulted in the decomposition of the plate form, with eventual formation of more massive types of carbide particles. After tempering at 400° F. the carbide residue was found by X-ray diffraction to contain the carbide type designated as Fe_2C. The Fe_3C type was also identified in residues from steels tempered at 600° F. (315° C.), and its pattern increased in intensity after tempering at higher temperatures up to 1000° F. or until the tendency toward an alloy carbide became evident. The Fe_3C pattern disappeared after tempering at 800° or 1000° F.

Since the change from plate to massive form took place after considerable development of Fe_3C and virtual disappearance of Fe_2C indications in the X-ray pattern, it was presumed that Fe_3C had become unstable with respect to Fe_3C, but that the transition was not accompanied by a change in the form of the carbide, and that this pseudomorph of the transition carbide persisted to the temperature at which the massive carbide form of Fe_3C or a change to alloy carbide was produced. It is, of course, possible that the plate form is Fe_3C throughout the range of its presence, and that its X-ray indication is suppressed by Fe_3C. However, this does not seem probable in view of the progressively better development of the plates after tempering up to 800° F. and the apparent absence of the massive type of particles. The conclusion that the change from the plate to the massive form coincides with the transition from the Fe_2C to Fe_3C type of carbide is, therefore, questionable.

The intercarbide film is of considerable interest, as the particles in extracted residues are associated in a way that suggests that the film may be of more significance than a grain boundary. In our work it was considered to represent a sort of umbilical connection between massive carbide particles, that was left as a vestigial trace of the plate form after the massive carbide particles had developed. This film may explain the exceedingly rapid initial growth of the massive type of particles, and the abrupt softening after the peak of secondary hardening is passed, in alloy steels. However, if this film is simply a grain or cell boundary it should be quite evident after tempering in the range that produces the plate form of carbide. The illustrations do not confirm this, and more study of the subject would appear to be justified.

These results have greatly increased our assurance of the fidelity with which electrolytically extracted residues represent the carbides in steel. It is to be hoped that the authors will continue their investigation, as the electron microscope seems to be a tool that is very difficult to use, but one that is capable of greatly extending our knowledge of these structures, and it is evident that they have used it very effectively.

AUTHORS' REPLY

Mr. **Trotter** and Mr. **McLean** (National Physical Laboratory) wrote: It is encouraging to note that Mr. Crafts and Mr. Lamont agree with us about the change in shape of the carbide particles from a plate form to a more massive form, but we differ about the temperature range in which this occurs; it is uncertain to what this difference is to be ascribed.

Our electron micrographs are not inconsistent with the suggestion that both the compounds Fe_2C (as Crafts and Lamont tentatively describe it) and Fe_3C (in its early stages) are plate-like, and that the change from plate to massive form is a change of shape of Fe_3C particles, without a composition change. The X-ray and electron diffraction work referred to in the introduction to the paper has now been briefly reported[2] and, like recent work by Jack,[3] supports this view.

In work subsequent to this paper, frequent examples of ridges such as those shown in Figs. 7 and 8, and of grain interior ridges, have been observed in steels in a variety of heat-treatment conditions.[4] Because they are found when the carbide has presumably not passed through the plate-like form—for example, in normalized specimens—it is thought that they are not essentially associated with this form, but rather that they occur at lattice misfits such as grain or sub-grain boundaries.

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* *Journal of The Iron and Steel Institute*, 1949, vol. 163, Sept., pp. 9–13.

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B. I. S. R. A.

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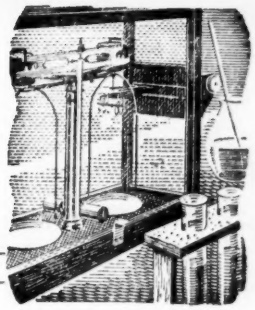

Cementiferous Paints

By J. E. O. Mayne, Ph.D., F.R.I.C., A.R.C.S., D.I.C.

Part II—ZINC PHOSPHATE CEMENTS

SYNOPSIS

The development of cementiferous paints based on the reaction between zinc dust and certain phosphate solutions is described. The paints dry by the formation of a zinc phosphate cement, and sufficient metallic zinc is left to afford cathodic protection to steel. The paints based on di-potassium and tri-potassium phosphate solutions dry at both high and low humidities. Laboratory tests, by immersion in natural sea water, indicate that they are superior to the earlier types of cementiferous paints based on chlorides, when applied to both pickled and rusty surfaces, as the zinc dust has a longer effective life. The paints are cheap and easy to prepare, but although they have an appreciable storage life, it is recommended that they should be used within a few hours of mixing.

Introduction

CEMENTIFEROUS paints, based on the reaction between zinc dust and certain chloride solutions, have been described in a previous publication.[1] These paints set by the formation of zinc oxychloride cement, which is unstable in the presence of water; Pyefinch[2] has shown, however, that this disadvantage can be overcome by the use of organic sealing coats.

Zinc oxychloride cements were formerly used in dentistry for the preparation of fillings, but, owing to their lack of water resistance, they have been replaced by zinc phosphate cements. The present investigation was therefore undertaken in order to discover whether suitable cementiferous paints could be developed, based on zinc phosphate cement, as it was felt that such paints might prove superior to oxychloride paints for two reasons:

(i) They would have an improved water resistance

(ii) They would be free from chloride ions and their effective life might be prolonged.

PRELIMINARY INVESTIGATION

Phosphate cements for dental use are prepared by mixing zinc oxide with a solution of phosphoric acid[3]; both ingredients contain minor constituents which control the physical properties of the final cement. Attempts to make a paint by mixing zinc dust with phosphoric acid solution were unsuccessful, as the mixture set too rapidly.

In order to investigate the effect of the *p*H of the phosphate solution upon the reaction with zinc dust, a 15% solution of orthophosphoric acid was mixed in the ratios 9/1, 8/2, 7/3, 6/4, and 5/5 with an equivalent solution of sodium hydroxide; in this way five solutions ranging in *p*H from 3 to > 10 were obtained. The *p*H value of each solution was roughly determined by means of B.D.H. Universal Indicator; later, when more accurate measurements were required, a glass electrode was used in conjunction with a Cambridge valve potentiometer. A measured volume (10 c.c.) of each solution was mixed in a mortar with a weighed quantity of zinc dust (54 g.), this proportion being similar to that used in the previous investigation.[1] A portion of the mixture was brushed on to a glass plate, and the remainder was allowed to stand in the mortar for 1 hr. and then a second glass plate was painted. Both plates were dried overnight and then tested by rubbing with a dry finger; the rubbing was then repeated under a stream of cold water from the tap. Cement formation was considered adequate if no paint was removed by either treatment, and the 'pot life' of the paint was judged to be satisfactory if both specimens passed these tests.

Two more sets of experiments were performed. In the first, a 15% solution of phosphoric acid was mixed with various proportions of an equivalent solution of potassium hydroxide, and in the second,

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Dr. Mayne is at the University Chemical Laboratory, Cambridge.

Table I

SOLUBILITIES OF THE PHOSPHATES OF SODIUM AND POTASSIUM

Compound	Temperature, °C.	Solubility of Anhydrous Material, g./100 g. of Saturated Solution
NaH_2PO_4	15	43·4[4]
	18	45·8[5]
	20	45·9[4]
Na_2HPO_4	15	4·6[4] 5·2[5]
	18	6·0[5]
	20	7·3[4] 7·7[5]
Na_3PO_4	10	7·6[5]
	20	10·8[5]
	25	12·3[5]
KH_2PO_4	10	15·5[5]
	15	16·9[5]
	20	18·5[5]
K_2HPO_4	22	63·6[6]
K_3PO_4	22	55·9[6]
	23	49·0[5]
	25	65·8[4]

the potassium hydroxide solution was replaced by an equivalent solution of ammonium hydroxide.

The following conclusions were drawn from this preliminary investigation :

(i) The ammonium phosphate solutions were quite unsuitable for the preparation of paints, as the mixture became unbrushable a few minutes after mixing

(ii) The sodium and potassium phosphate solutions yielded very promising paints when the *p*H of the solutions was greater than 8 ; more acid solutions set too rapidly in the mortar.

An examination was next made of the possible use of the sodium and potassium phosphates. Their solubilities are given in Table I, and the moisture content of each sample used in this investigation was obtained by the following procedure :

Weighed samples were heated in an oven maintained at 110° C. until they were of constant weight, and the percentage loss in weight is shown below :

	Loss in wt., %
Sodium di-hydrogen phosphate ...	25·2
Di-sodium hydrogen phosphate ...	58·5
Tri-sodium phosphate	49·8
Potassium di-hydrogen phosphate ...	0·4
Di-potassium hydrogen phosphate...	0·1–1·2
Tri-potassium phosphate	3·0–8·6

The sodium salts corresponded approximately to $NaH_2PO_4.2H_2O$ (23·1% H_2O), $Na_2HPO_4.12H_2O$ (60·3% H_2O), and $Na_3PO_4.10H_2O$ (52·3% H_2O).

The potassium salts were very hygroscopic and the moisture content increased with the age of the sample, but in no case did it exceed 10%, and therefore no allowance was made in preparing the solutions used in this investigation.

The Sodium Phosphates

Sodium Di-hydrogen Phosphate—A 20% solution of the dihydrate had a *p*H of 3·8, and when mixed with zinc dust set almost instantaneously.

Di-sodium Hydrogen Phosphate—A saturated solution of the dodecahydrate had a *p*H of 8·7, and it yielded a paint which could be removed easily by dry rubbing. This lack of cohesion was probably due to the solubility of this salt being too low to ensure adequate cement formation (*see* Table I).

Tri-sodium Phosphate—A saturated solution of the decahydrate formed paints which gave irreproducible results in the rubbing tests. Later it was found that excellent paints could be obtained if the solution was at 25° C., but that poor paints were obtained if the temperature was allowed to drop to 18° C. It was concluded that the solubility of the salt was just too low at normal room temperature for the production of satisfactory cementiferous paints.

The Potassium Phosphates

Potassium Di-hydrogen Phosphate—A saturated solution of this salt had a *p*H of 4·0, and it yielded an unbrushable paint when mixed with zinc dust.

Di-potassium Hydrogen Phosphate—Preliminary experiments indicated that satisfactory paints could be obtained with a 20% solution of this salt. These paints dried satisfactorily on glass and on pickled steel, but on rusty steel surfaces the paint was easily removed by gentle rubbing.

This lack of cohesion of the paint on rusty surfaces was at first thought to be due to the chemical action of iron oxide (rust). Experiments indicated, however, that this was not the case, and that the dry rust was merely absorbing the phosphate solution, thereby reducing the quantity available for reaction with the zinc dust. The trouble was finally overcome by the use of a 30% solution of di-potassium hydrogen phosphate. Storage trials with this paint have shown that, provided it does not dry by evaporation, the paint has a ' pot life ' of at least three days.

Tri-potassium Phosphate—A good paint was obtained with a 25% solution of this salt. This paint set satisfactorily on glass and pickled steel, but the concentration of the solution had to be increased to 35% in order to obtain a paint suitable for application to rusty surfaces ; this paint also had a pot life of at least three days.

The Effect of the *p*H of the Solution on the Speed of Setting

It has been shown that a solution of potassium di-hydrogen phosphate (*p*H 4) was unsuitable for the preparation of cementiferous paints, whereas a 30% solution of di-potassium hydrogen phosphate (*p*H 8·5) yielded a satisfactory product. In order to examine the possibility of the preparation of paints from solutions of intermediate *p*H, some experiments were carried out with mixed solutions of potassium di-hydrogen phosphate (saturated) and di-potassium hydrogen phosphate (30%). The results obtained were as follows :

*p*H of Phosphate Solution	Speed of Setting in Mortar
5·5	Set within 5 min.
6·5	Just set in 1 hr., solid in 2 hr.
7·3	Only just paintable after 1 hr., solid in 2 hr.

These results indicate that the phosphate solution should have a *p*H in the neighbourhood of 8, otherwise premature setting takes place.

THE EFFECT OF HUMIDITY ON THE SETTING OF PHOSPHATE PAINTS

The previous section of this report described the development of two phosphate paints. One was based on a 30% solution of K_2HPO_4, and the other on a 35% solution of K_3PO_4, and in both cases 54 g. of zinc dust were ground in a mortar with 10 c.c. of the phosphate solution. The effect of the humidity of the atmosphere on the rate of drying of these two paints was now examined.

Both paints were applied to glass, pickled steel, and rusty steel, and allowed to dry in the laboratory on days when the relative humidity was 42%, 60%, 67%, 82%, and 86%. The relative humidity was measured by means of a wet-and-dry-bulb hygrometer, which was placed in front of an electric fan so that the air velocity was greater than 3 m./sec. In every case the paint was dry 1 hr. after it had been applied.

In another series of experiments the paints were dried : (i) In front of an electric fan ; (ii) in a desiccator vessel containing calcium chloride ; (iii) in a dessiccator vessel containing water. All the paints dried satisfactorily, the slowest being dry 70 min. after application. It was concluded that the setting of these paints was not appreciably affected by the relative humidity of the atmosphere, and that they could be applied in wind or at very high humidities, such as those which prevail in a dry dock.

THE MECHANISM OF SETTING OF ZINC PHOSPHATE CEMENTS

Cromwell[7] has attributed the setting of a mixture of zinc oxide and orthophosphoric acid to the formation of secondary and tertiary hydrated zinc orthophosphates, $ZnHPO_4.3H_2O$ and $Zn_3(PO_4)_2.4H_2O$. This view has been partially confirmed by Halla and Kutzelnigg,[8] who carried out an X-ray examination on a mixture of zinc oxide and 89% orthophosphoric acid solution. They found lines corresponding to the compound $Zn_3(PO_4)_2.4H_2O$, together with free zinc oxide ; but the acid salt was absent.

An extensive examination of the natural and basic phosphates of the heavy metals has been made by Rathje,[9] during the course of which he prepared $3Zn_3(PO_4)_2.Zn(OH)_2$, and he suggested that this compound plays a part in the setting of dental cements.

The setting of a mixture of zinc dust and a phosphate solution having a $pH > 8$ cannot be attributed to the formation of an acid phosphate, and it is therefore probable that the setting is due to the formation of $Zn_3(PO_4)_2.4H_2O$, or $3Zn_3(PO_4)_2.Zn(OH)_2$. It appears likely that the potassium phosphate solution first reacts with the zinc oxide, which is always present in zinc dust, with the formation of zinc phosphate, potassium hydroxide, and water. This reaction can only proceed for a short time owing to the limited supply of zinc oxide, and it is therefore probably succeeded by a reaction between the zinc dust and potassium phosphate, which yields zinc phosphate, potassium hydroxide, and hydrogen. This reaction is slow when the paint is stored in bulk, probably owing to the fact that the hydrogen is not readily removed from the surface of the zinc : when, however, the paint is spread out in a thin film, the zinc is depolarized by atmospheric oxygen and rapid setting of the paint ensues. Secondary reactions may take place between the zinc dust and the potassium hydroxide with the formation of complex zincates, although if this does occur, it does not apparently affect the setting reaction.

This explanation of the setting reaction receives confirmation from the following experimental facts :

(i) Phenolphthalein paper was not affected by a 30% solution of di-potassium phosphate ; upon mixing the solution with zinc dust the test paper turned purple, showing that the pH of the solution had increased

(ii) Paints made from both di-potassium and tri-potassium phosphate evolved hydrogen when stored in a closed container. To measure the volume of gas evolved, mixtures were placed in a sealed tube connected with an Ostwald gas burette which was in a thermostat at 30° C. Results obtained over a period of six days were as follows :

Paint	Total volume (c.c.) of gas evolved/g. of paint after 1	2	3	4	5	6 days
K_2HPO_4	0·3	2·0	4·6	7·4	10·2	13·1
K_3PO_4	2·1	5·7	9·2	12·7	16·0	—

(iii) Paints made from both di-potassium and tri-potassium phosphate were stored in covered containers for more than three days at room temperature. From time to time water was added to the mixture and a portion of the paint was applied to glass. All samples set within an hour and could not be removed by dry or wet rubbing with the finger. It is possible that the protective properties of the paints were altered by this period of storage ; this point was not examined, as it is considered that these paints should always be freshly made.

THE THICKNESS OF PHOSPHATE PAINT COATINGS

When both phosphate paints were well brushed out on pickled and rusty steel surfaces, the weight of the dried film was 0·4–0·8 oz./sq. ft. Satisfactory coatings as thick as 1·6 oz./sq. ft. have been obtained by liberal application of the paints. If, however, the paints were made up with a phosphate solution that was too strong, or were stored too long at a high temperature, they could only be applied with difficulty, and the weight of the coating rose to 2·7 oz./sq. ft. Moreover, the coating blistered, cracked, and flaked off on drying ; this failure was probably due to the vigorous nature of the setting reaction. It should be noted that the effects of storage can be overcome by the addition of water.

COMPARISON OF PHOSPHATE PAINTS WITH OXYCHLORIDE PAINTS

In the preliminary experiments, steel specimens which were in five different surface conditions were used. They were cut from a coil of hot-rolled mild steel and treated in the following ways : (i) Pickled ; (ii) sand blasted ; (iii) weathered on the roof of the Cambridge Laboratory until they had lost about half their scale ; (iv) weathered for about one year—a treatment which removed all the scale ; (v) weathered

for about four years—these specimens were covered with a thick adherent layer of rust. All the weathered specimens were wire brushed before painting.

A paint, prepared by grinding zinc dust with a 30% solution of di-potassium hydrogen phosphate, was applied to all five surfaces, and allowed to dry overnight. The paint coat was then broken by a scratch mark, and the specimens were partially immersed in natural sea water contained in small beakers; the sea water was changed weekly. A few rust spots appeared on the rusty specimen carrying broken scale after 49 days; the attack, however, did not spread, so that after immersion for six months all the specimens were in good condition, rust being absent from the sea water and the scratch lines.

These results appeared so promising that a comparison was made with an oxychloride paint (E 412), which was applied in duplicate to pickled (i) and very rusty (v) specimens. Within 24 hr. of immersion rust appeared at the scratch line on the rusty specimens, and after immersion for 14 days the painted surface of the rusty specimens was covered with rust spots.

These experiments indicated that on rusty surfaces the phosphate paints were superior to the oxychloride paints.

The mechanism of inhibition by cementiferous paints has been examined in a previous publication,[1] and it has been shown that a study of the changes in the potential of the specimen over a period of time provides an assessment of protective value. Experiments on this basis were therefore carried out to obtain further information regarding the relative value of phosphate and oxychloride paints on specimens immersed in sea water. The specimens were in the pickled (i) and very rusty (v) condition, and they were painted with :

(*a*) An oxychloride paint (E 412)
(*b*) Zinc dust mixed with a 30% solution of di-potassium hydrogen phosphate
(*c*) Zinc dust mixed with a 35% solution of tri-potassium phosphate.

The paints were allowed to dry, the coating broken by a scratch mark, and the specimens partially immersed in natural sea water which was changed weekly. The potential of each specimen was measured, during a period of 100 days, by means of a Cambridge valve potentiometer together with a standard calomel electrode. The experiments were performed in duplicate, and the results obtained are shown in Table II.

Previous work has established the fact that the protection afforded by cementiferous paints is associated with the establishment of metallic contact between the zinc dust in the paint and the underlying steel. It is difficult to establish metallic contact through rust, and it is for this reason that the oxychloride paints have given variable results on rusty surfaces.

A piece of zinc, immersed in sea water, takes up a potential of about — 1·040 V., and it can be seen from Table II, section *A*, that a pickled surface coated with a cementiferous paint takes up a similar potential when immersed in sea water. The potential of a steel specimen, coated with a zinc-rich paint, will tend to rise as soon as the metallic contact between the zinc and the steel is destroyed, until it reaches a value in the range — 0·600 to — 0·700 V., which is the potential of uncoated steel corroding in sea water. If the rise in potential is rapid, the coated steel specimen will corrode, but if the specimen remains for some time at a potential of about — 1·000 V., an inhibitive film will be formed, which will prevent subsequent attack. It follows, therefore, that the most effective paints are those which maintain the potential of the specimen in the region of — 1·000 V. for the longest possible time.

Accordingly, it can be seen from Table II that the phosphate paints are more effective than the oxychloride paints on both pickled and very rusty surfaces; there is some evidence that the paint prepared with the di-potassium phosphate is superior to that prepared with the tri-potassium phosphate, but the effect is not very great.

POSSIBLE APPLICATIONS OF PHOSPHATE PAINTS

It has been found that dried films of phosphate paints are alkaline to phenolphthalein paper. This alkalinity did not affect the drying of linseed oil, since a sample of alkali-refined linseed oil, containing 0·40% of Pb and 0·08% of Mn in the form of naphthenates, dried at the same rate on glass as on both types of phosphate paints.

Pyefinch[2] has shown, as a result of raft trials, that cementiferous paints of the oxychloride type have a possible application as priming coats for the protection of steel against immersion in sea water. Laboratory experiments have indicated that the phosphate paints should be superior to the earlier paints, but this conclusion must be confirmed by raft and service trials.

Preliminary atmospheric exposure tests on phosphate paints in Cambridge have given promising results, and it is clear that on rusty surfaces, under these conditions of exposure, they are superior to the oxychloride type of paints. The phosphate paints might form the basis of a wash for application to pickled steel in order to prevent the formation of rust before paint is applied. Another possible use is for the prevention of stress-corrosion of aluminium alloys.

COST OF PHOSPHATE PAINTS AND POSSIBLE DEVELOPMENTS OF CEMENTIFEROUS PAINTS

A current quotation (June, 1949) for zinc dust is £89/ton, di-potassium hydrogen phosphate 3*s.* 3*d.*/lb., and tri-potassium phosphate (80%) 4*s.* 6*d.*/lb. The composition of the paint is zinc dust 81%, potassium phosphate 6–7%, water 12–13%; thus the cost of the paint prepared from di-potassium hydrogen phosphate is 11*d.*/lb., and that based on tri-potassium phosphate 1*s.* 1*d.*/lb. A pound of either will cover 10–40 sq. ft. according to the thickness of the coat. The paint is easy and cheap to manufacture, since the ingredients only require mixing, provided that the zinc dust is free from lumps; but difficulties may be experienced in the preparation of large quantities in a single operation owing to the weight of the zinc dust.

It has been established that in order to form a

Table II

CHANGES IN POTENTIAL OF COATED SPECIMENS WITH TIME

The specimens were partially immersed in natural sea water which was changed once a week. Reference electrode : Calomel/saturated potassium chloride.

Time, days	*A* Pickled Specimens Potential, volts (negative)						*B* Very Rusty Specimens Potential, volts (negative)					
	$Zn-K_2HPO_4$ (30%) Solution		Paint E 412		$Zn-K_3PO_4$ (35%) Solution		$Zn-K_2HPO_4$ (30%) Solution		Paint E 412		$Zn-K_3PO_4$ (35%) Solution	
0	1·193	1·205	1·070	1·070	1·227	1·165	0·971	0·972	0·292	0·294	0·899	0·923
5	1·057	1·045	0·771	0·773	1·046	1·032	0·996	0·995	0·534	0·547	0·949	1·037
10	1·062	1·054	0·752	0·787	1·041	1·045	1·012	1·006	0·553	0·565	0·957	1·019
15	1·058	1·090	0·714	0·690	1·044	1·048	1·016	1·006	0·586	0·594	0·954	1·013
20	1·060	1·030	0·696	0·751	1·048	1·051	1·006	1·052	0·602	0·604	0·950	1·006
25	1·055	1·061	0·716	0·793	1·038	1·043	1·011	0·995	0·610	0·616	0·940	1·004
30	1·056	1·058	0·726	0·696	0·975	0·976	0·979	0·943	0·625	0·621	0·880	0·936
40	1·053	1·056	0·756	0·777	1·023	1·021	0·939	0·914	0·642	0·633	0·814	0·936
50	1·057	1·053	0·701	0·737	1·006	1·007	0·934	0·892	0·637	0·638	0·784	0·874
60	1·036	1·035	0·793	0·735	...	...	0·868	0·873	0·649	0·629	...	...
70	1·029	1·034	0·765	0·850	0·995	0·990	0·822	0·851	0·715	0·642	0·696	0·766
80	1·028	1·027	0·738	0·759	0·975	0·982	0·814	0·818	0·724	0·635	0·619	0·691
90	1·026	1·032	0·696	0·716	0·889	0·910	0·793	0·793	0·680	0·606	0·496	0·541
100	1·001	1·001	0·786	0·703	0·970	0·966	0·763	0·775	0·782	0·622	0·561	0·544

satisfactory cementiferous paint a phosphate solution must :

(i) Be sufficiently strong to ensure adequate cement formation

(ii) Have a *p*H value greater than 8, otherwise premature setting of the paint takes place.

Both of these conditions might be fulfilled by solutions containing a mixture of sodium and potassium phosphates. No information is available regarding the solubilities of mixed phosphates ; but suitable solutions having a *p*H of 8·5 could be obtained from the following mixtures, *viz.*, $Na_2HPO_4 + K_2HPO_4$, $NaH_2PO_4 + K_3PO_4$, $KH_2PO_4 + Na_3PO_4$, and for the paints prepared from tribasic salts a mixture of Na_3PO_4 and K_3PO_4 might be used. Since sodium salts are not as expensive as potassium salts the resulting paint would be cheaper, but calculation indicates that this price would only drop by about 1*d.*/lb. ; for this reason this possibility has not yet been investigated.

At the commencement of this investigation an examination was made of the literature relating to the chemistry of dental cements ; in addition to oxychloride and phosphate cements two other cements which have been used in dentistry might form the basis of cementiferous paints :

(i) Silicate cements, prepared by mixing a fused and ground silicate with a solution of phosphoric acid. It is possible that satisfactory cementiferous paints might be prepared by mixing zinc dust with sodium silicate solution.

(ii) Copper cements, which consist of various proportions of cuprous oxide, iodide or silicate, cupric oxide, and zinc oxide, mixed with a solution of phosphoric acid, and thus they might form the basis of an antifouling paint.

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Blast-Furnace Gas Cleaning

METHODS FOR CALCULATING THE MOTIONS OF PARTICLES IN A GAS

By J. Stringer, B.A.

SYNOPSIS

Curves are given by means of which many problems concerning the behaviour of particles in a gas stream may be solved, at least in part. Of particular advantage in the analysis of tests on blast-furnace gas-cleaning plant, is a nomogram by means of which measurements of particle free-falling speeds may be extrapolated to give the terminal velocity under the conditions existing within the plant. It is considered that this will give much more accurate results than those obtained by the usual method of applying Stokes' law and extrapolating far beyond its limit of validity.

In most methods of removing solid particles from a gas, a force, which may be gravitational, centrifugal, or electrostatic, is applied to increase the momenta of the particles and, by creating a difference between the velocities of gas and particle, to effect separation. In order, therefore, to assess the limitation of gas-cleaning methods it is necessary to calculate the motion of a particle when acted upon by a field of force which is opposed by the viscous drag of the gas. A further similar problem is that of calculating the penetration of a particle (*e.g.*, a water drop) which is projected into a gas; this may be applied to the behaviour of spray scrubbing towers.

This paper gives curves and nomograms to facilitate the calculation of these particle motions, and forms in a sense a mathematical appendix to a survey* of methods of cleaning blast-furnace gas. A summary of the methods derived and explanations of the curves and nomograms is given later in the paper (*see* p. 295), and will enable the results to be used without referring to the mathematical arguments.

The calculations are made only for the case of an isolated particle moving in a straight line. In gas-cleaning problems, particle concentrations are low, so that interference between particles is negligible. Problems of motion in paths other than the rectilinear can be treated only by *ad hoc* step-by-step methods, except that where Stokes' law applies, the resistance may be split-up vectorially and in proportion to the velocity. The motions in two perpendicular directions may then be treated separately and the solutions may be added vectorially.

DRAG ON PARTICLES

The law of resistance of spherical particles is well established and can be represented by a unique curve of resistance coefficient as a function of Reynolds number, where resistance coefficient is defined as:

$$C_D = \frac{8 \times \text{resistance}}{\pi d^2 \rho V^2}$$

Such a curve, taken from a paper by Castleman† (1924) is plotted on logarithmic scales in Fig. 1. It will be seen that the curve falls into three sections:

(*a*) A straight line with a slope of 45° for Reynolds numbers ($R = Vd/\nu$‡) less than 0·5, when the resistance is due almost entirely to viscous forces, and Stokes' law applies, *viz.*:

$$C_D = \frac{24}{R} \qquad \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots (1)$$

(*b*) A horizontal straight line corresponding to $C_D = 0{\cdot}40$ for Reynolds numbers greater than 2500, in which range fluid inertia forces predominate.

(*c*) A transition range, $R = 0{\cdot}5$ to $R = 2500$, in which both types of force have their effect.

The particle motions met with in gas-cleaning problems fall in categories (*a*) and (*c*).

The laws for non-spherical particles may be expected to be similar, except that the orientation of the particle to the direction of motion may affect the resistance coefficient and, above a Reynolds number in range (*b*) or (*c*), the particle may spin owing to the generation of vortices in the fluid.

Experiments on isometric particles by Pettyjohn and Christiansen§ (1948) lead to a series of resistance curves for various shapes (tetrahedron, cube, octahedron, etc.). These are similar to those for spheres up to $R = 150$ (for cubes) and $R = 250$ (for octahedra), but with slightly higher resistance coefficients. It appears from the curves (Fig. 1) that the increase can be allowed for by the introduction of a constant factor (K) dependent on the sphericity (defined below) of the particle. There appears to be empirical justification for extending the use of their formula, which is only strictly applicable for $R < 0{\cdot}5$:

$$\frac{1}{K} = 0{\cdot}843 \log_{10} \frac{\psi}{0{\cdot}065} \qquad \ldots\ldots\ldots\ldots\ldots\ldots (2)$$

where K is the factor (greater than unity) by which

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* R. F. Jennings, *Journal of The Iron and Steel Institute*, 1950, vol. 164, Mar., p. 305 (*this issue*).

† R. A. Castleman, National Advisory Committee for Aeronautics (U.S.A.), 1924, Technical Note No. 231.

‡ A list of symbols is given on p. 298.

§ E. S. Pettyjohn and E. B. Christiansen, *Chemical Engineering Progress*, 1948, vol. 44, Feb., p. 157.

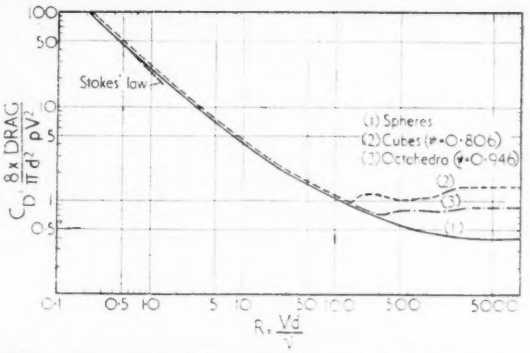


Fig. 1—Drag curves for spheres, cubes, and octahedra

the resistance coefficient for the equivalent sphere must be multiplied. Formula (2) is presented graphically in Fig. 2.

Sphericity (ψ) is defined as 'the surface area of a sphere having the same volume as the particle, divided by the surface area of the particle' ($\psi = 0{\cdot}806$ for cubes, for example). Also, the diameter used in the drag equations and in calculating the Reynolds number must be that of the equivalent sphere (*i.e.*, that with the same volume as the particle).

Formula (2), whilst justifiable only in the range of viscous flow (*i.e.*, under Stokes' law conditions), applies empirically up to the following limits :

(i) Values of R less than 150 if ψ is greater than $0{\cdot}8$

(ii) Values of R less than 250 if ψ is greater than $0{\cdot}95$

(iii) All values of R if $\psi = 1$.

Above these limits particle shape has a different effect from this simple relationship which expresses the fact that the viscous drag at low velocities is a function of the surface area of the particle and not of its orientation and ability to shed vortices. In the completely turbulent region the resistance coefficient may be three times that of the equivalent sphere owing to the larger cross-sectional area presented to the flow and the consequent greater generation of vortex energy.

There is no reliable method of estimating the drag on an irregular particle, particularly if it is of vesicular form. The foregoing suggests, however, that the introduction of a shape factor may be all that is necessary to apply results for spheres to other compact, but more irregular, shapes in ranges (*a*) and (*c*). It is probable that the shapes of the dust particles in dirty blast-furnace gas are generally not of elongated shape, and the shape-factor method of estimating drag may well be applicable. It is shown in Appendix III to this paper that when dust concentration and size are measured by a separation method depending on the free falling speed under standard conditions, only an approximate estimate of the value of the shape factor is required.

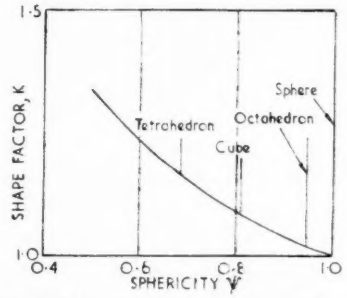


Fig. 2—Variation of shape factor with sphericity (isometric particles, according to Pettyjohn and Christiansen)

Throughout the paper whenever the diameter of a particle is mentioned the diameter of the equivalent sphere is implied, *i.e.*, the 'diameter' of a particle is equal to :

$$\left(\frac{6 \times \text{volume}}{\pi}\right)^{\frac{1}{3}}$$

EXPLANATION OF METHODS DERIVED

In Appendices I and II will be found the derivation of curves for evaluating the time taken and the distance travelled by a particle moving in a gas under the influence, in the one case, of a field of acceleration and a viscous drag, and in the other, of only the drag. In each case a measure of generality is achieved by expressing all the variables in groups of dimensionless parameters. This allows the calculations to be made only once for a series of values of these parameters which can then be used to cover a wide range of conditions. The chief parameters employed are :

(1) Reynolds number (Vd/ν) as a measure of velocity.

(2) G, a measure of the accelerating force related to size of particle :

$$= \frac{4\sigma f d^3}{3\rho\nu^2 K}$$

(3) T_f, a measure of elapsed time :

$$= \frac{\text{Time} \times \rho\nu K}{\sigma d^2}$$

(4) S_f, a measure of distance travelled :

$$= \frac{\text{Distance} \times \rho K}{\sigma d}$$

The calculation of these parameters is facilitated by nomograms (Figs. 11–14) by which the particular values in any problem may be converted. In these nomograms average values of density and kinematic viscosity of air and blast-furnace gas are used, the values being functions of gas temperature alone. It is, of course, necessary to know also the equivalent spherical diameter, the specific gravity, and the shape factor of the particle in question. In general, these may only be estimated except, perhaps, in the case of water sprays when $\sigma = K = 1$ and only the diameter is required. A method of procedure in the case when particles are identified only by differences in their free falling speeds, as in making a practical test of a gas-cleaning system, is outlined below.

The application of the curves relevant to two types of problem will be considered separately.

Accelerated Motion and Terminal Velocity (Appendix I)

The problem to be considered is that of the motion of a particle relative to a gas when subjected to a

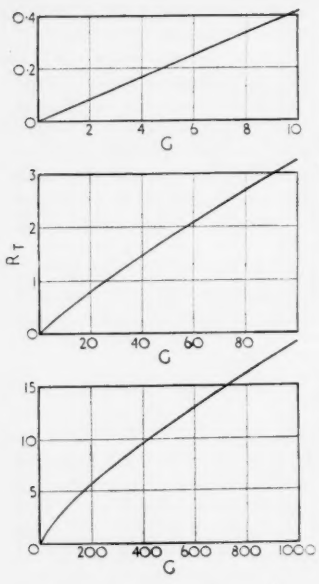


Fig. 3—Terminal Reynolds number as a function of *G*

field of force acting in one direction. This field of force may be gravitational, centrifugal, or electrostatic, or may be due to acceleration or deceleration of the gas. Since drag increases with velocity, at some point in the motion of a particle the drag just equals the accelerating force and there is no further acceleration. The velocity at which this occurs is known as the terminal velocity, at which velocity most of the motion takes place. The terminal velocity may be derived from Fig. 3 as a value of the Reynolds number corresponding to a given value of *G*.

Before the terminal velocity is reached, the particle is accelerating, and the position and time corresponding to any velocity (less than the terminal velocity) may be derived from Figs. 4 and 5, again as dimensionless parameters. In many cases this transient range is not of great interest and calculation of the terminal velocity is sufficient. To clarify this point, curves (Figs. 6 and 7) have been plotted giving the non-dimensional equivalents of the distance–time curves. Those curves may be useful in helping to decide whether, in a particular case, a particle has time to acquire its terminal velocity or whether a more detailed analysis is required. A simple equation

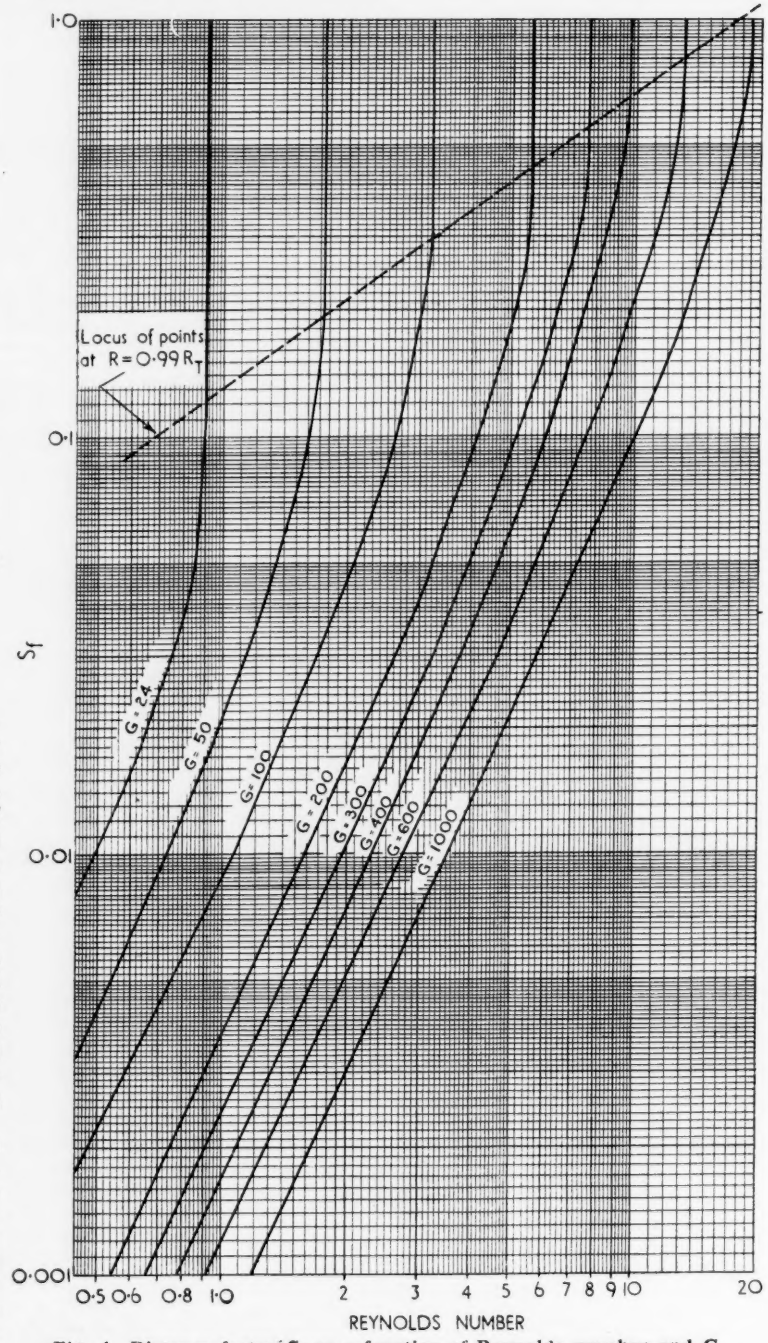


Fig. 4—Distance factor S_f as a function of Reynolds number and *G*

for the motion at the terminal velocity is given in Appendix I, equation (24).

Non-Accelerated Motion (Appendix II)

In Appendix II formulae and curves are derived for calculating the distance gone and the time taken for drag to reduce the relative velocity of particle and

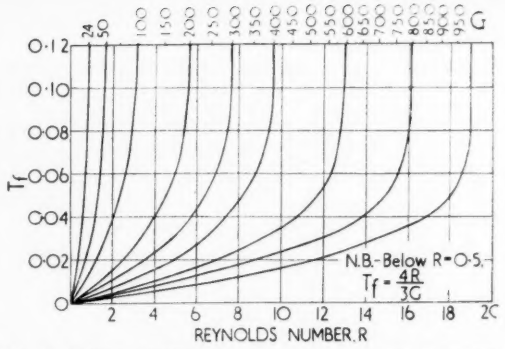


Fig. 5—Time factor T_f as a function of Reynold's number and G

gas to zero or, by a simple extension, to any other given value. This is a special case of Appendix I in which the accelerating force is zero, and a closer approximation to the actual drag curve is possible, making for greater accuracy.

The result is expressed as two simple curves (Figs. 8 and 9) of time and distance parameters as functions of Reynold's number. To find the time or distance taken in losing velocity from one value to another, subtract the parameter values corresponding to the true velocities.

Determination of Terminal Velocity

Particle size is commonly determined by settlement or by elutriation. In each of these methods the decisive quantity is the free falling speed under the action of gravity ; in the elutriation method this speed is balanced against that of an upward-moving stream of air.

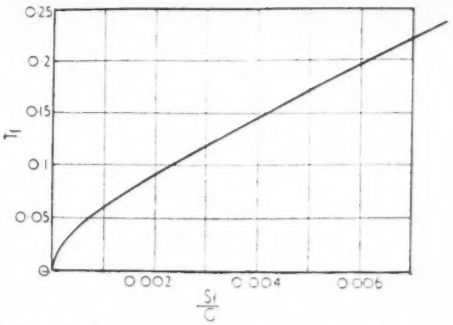


Fig. 6—$S_f G$ plotted against T_f in Stokes' law range

A nomogram is included (Fig. 16) on which the conditions in the elutriator are combined to give a value on the right-hand transfer line which is a unique property of the particle.

Increasing Acceleration

In many applications the accelerating field is not constant. In such cases it is possible to apply the present methods by using a step-by-step approximation, the acceleration being assumed constant over each step. Where the acceleration increases from a small value during the motion, sufficient accuracy may be obtained by assuming that at each point the velocity is the terminal velocity corresponding to the acceleration at the point. The greatest error in this approximation will arise with large, heavy particles and with high accelerations. To examine the validity of this assumption a particular case has been worked out in which the force on a 100-μ particle, of specific gravity 4, increases from gravity to 11 times gravity

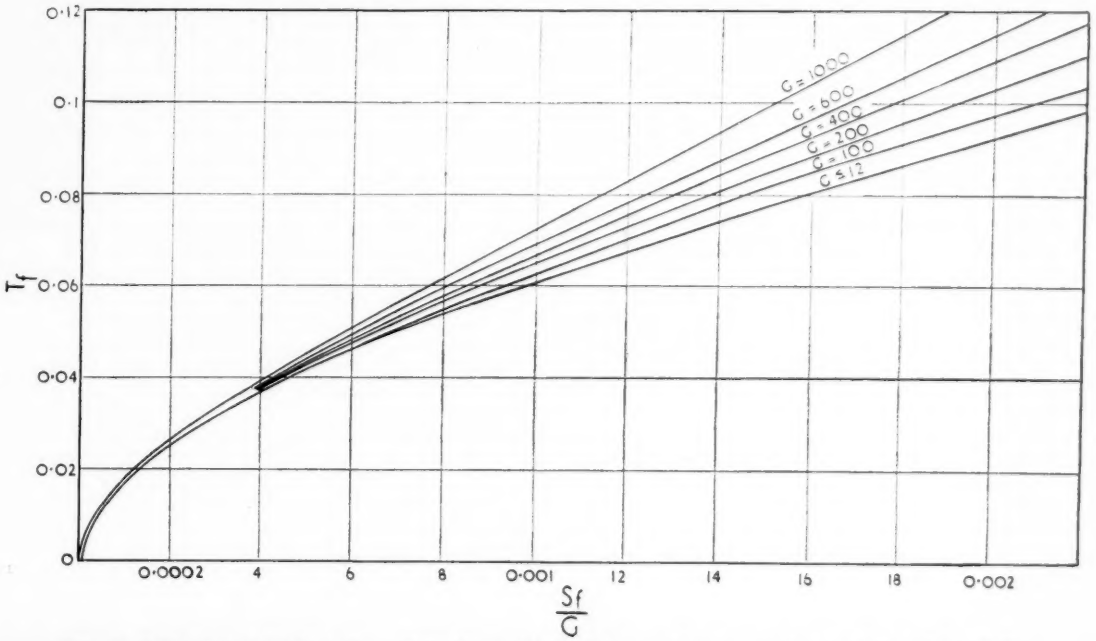


Fig. 7—$S_f G$ plotted against T_f. Non-dimensional distance–time curves for various values of G

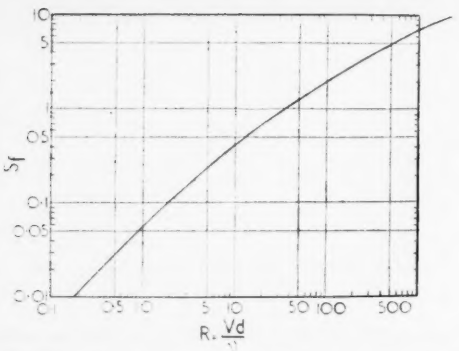


Fig. 8—Distance factor S_f for a particle to come to rest from a given Reynolds number. No accelerating force

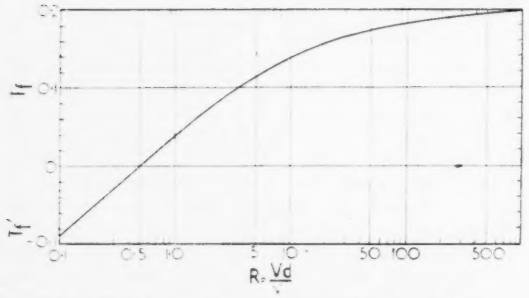
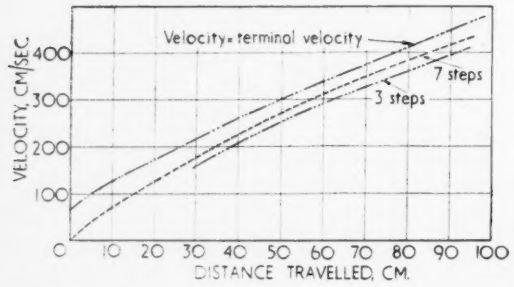


Fig. 9—Time factors corresponding to various Reynolds numbers. No accelerating force



Particle : 100-μ sphere, sp.gr. = 4
Gas : Blast-furnace gas at 300° C.
Accelerating force : Gravity at 0 cm., 11 × gravity at 100 cm.

Fig. 10—Comparison of 'step' and 'terminal velocity' methods of calculating velocity when accelerating force increases with distance

in ½100 cm. This has been done both by the step-by-step method and by assuming that velocity equals terminal velocity. The results are plotted in Fig. 10, from which it can be seen that increasing the number of steps in the calculation moves the solution in the direction of that based on the terminal velocity, so that the latter may be a fair approximation in many cases. The true curve will, of course, start at the origin and lie slightly below the terminal velocity curve.

LIST OF SYMBOLS

Properties of the Particle

d = diameter, or diameter of the equivalent sphere $= \left(\frac{6 \times \text{volume}}{\pi}\right)^{\frac{1}{3}}$
K = 'shape factor,' relation between drag coefficient and that of equivalent sphere
m = mass
V = velocity
V_T = terminal velocity
ψ = sphericity (defined on p. 295)
σ = density
μ = micron(s) ($1\mu = 10^{-4}$ cm.)

Properties of the Gas

ν = kinematic viscosity
ρ = density
η = absolute viscosity

Drag and Acceleration

a = coefficient in approximate drag formula : $C_D = a/R^n$
a_o = coefficient for equivalent sphere
C_D = resistance coefficient $= \left(\frac{8 \times \text{resistance}}{\pi\rho V^2 d^2}\right)$
C_{D_o} = resistance coefficient of equivalent sphere
n = index in approximate drag formula : $C_D = a/R^n$
f = strength of accelerating field (acceleration of a particle *in vacuo*)
x = distance
t = time

Dimensionless Ratios

$B = \frac{4}{3}\frac{fd^3\sigma}{\rho\nu^2}$
b = constant in formula for motion at terminal velocity
$G = B/K = \frac{4fd^3\sigma}{3\rho\nu^2 K}$
$H = \frac{4}{3}\frac{\sigma f\nu}{K\rho V_T^3}$
R = Reynolds number Vd/ν
R_T = terminal Reynolds number = $V_T d/\nu$
S_f = distance factor = distance travelled × $\rho K/\sigma d$
T_f = time factor = time taken × $\rho\nu K/\sigma d^2$
T_f' = time factor to come to rest from $R = 0{\cdot}5$

APPENDIX I

ACCELERATED MOTION AND TERMINAL VELOCITY

General Equation

The problem to be considered is the motion of a particle relative to a gas when subjected to a field of force acting in one direction. This field of force may be gravitational, centrifugal, or electrostatic, or may be due to the acceleration or deceleration of the gas. Assuming the force on the particle (of mass m grams, pounds, or slugs) to be mf (dynes, poundals, or pounds force, respectively), Newton's second law gives as the equation of motion :

$$m\frac{d^2x}{dt^2} = mf - \text{resistance} \quad \ldots\ldots(3)$$

or

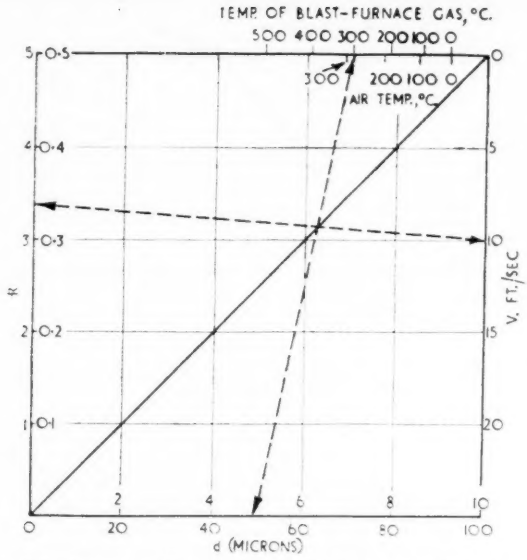
$$\frac{\pi\sigma d^3}{6}\left(\frac{d^2x}{dt^2} - f\right) + \frac{\pi}{8}C_D\rho V^2 d^2 = 0 \quad \ldots\ldots(4)$$

for a spherical particle.

It will be convenient to maintain all the variables as dimensionless groups. Replacing V by the group

$$V = \frac{\nu R}{d}$$

gives



N.B.—Use corresponding scales for R and d, *i.e.*, 0–100 μ with R = 0–5, 0–10 μ with R = 0–0·5

Fig. 11— Nomogram for Reynolds number of a particle in blast-furnace gas or air

$$\frac{4\sigma d^3}{3\rho\nu^2}\left(\frac{d^2x}{dt^2} - f\right) + C_D R^2 = 0 \quad \ldots\ldots(5)$$

Further, if f is a constant, or can be considered so over a range of distances, it is convenient to use the dimensionless group :

$$B = \frac{4f\sigma d^3}{3\rho\nu^2} \quad \ldots\ldots(6)$$

so that equation (5) becomes :

$$B\left(\frac{1}{f}\frac{d^2x}{dt^2} - 1\right) + C_D R^2 = 0 \quad \ldots\ldots(7)$$

It has been mentioned (p. 294) that, at Reynolds numbers up to 150, the drag of a non-re-entrant particle may be expected to be equal to that of the equivalent sphere, multiplied by a shape factor K, so that if C_{D_o} is the drag coefficient of the equivalent sphere, then :

$$C_D = K.C_{D_o};$$

so that equation (7) becomes :

$$G\left(\frac{1}{f}\frac{d^2x}{dt^2} - 1\right) + C_{D_o}R^2 = 0 \quad \ldots\ldots(8)$$

where $$G = \frac{B}{K} = \frac{4\sigma f d^3}{3\rho\nu^2 K} \quad \ldots\ldots(9)$$

Terminal Velocity

Since drag increases with velocity, at some point in the motion of a particle the drag just equals the accelerating force and there is no further acceleration. The velocity at which this occurs is known as the terminal velocity and is found by equating d^2x/dt^2 to zero in equation (8). Thus, if R_T is the Reynolds number at the terminal velocity, then

$$G = C_{D_o}R_T^2 \quad \ldots\ldots(10)$$

Where Stokes' law applies (*i.e.*, $C_D = a/R$), this leads to a terminal velocity, given by the Reynolds number :

$$R_T = G/a_o \quad \ldots\ldots(11)$$

In the intermediate range, R_T must be found by successive approximation. Values have been worked out from the drag curve for spheres and are plotted on logarithmic scales in Fig. 3.

Time–Velocity Relationship

Equation (8) can be integrated to give a time–velocity relationship if the following substitution is made :

$$\frac{d^2x}{dt^2} = \frac{dV}{dt} = \frac{\nu}{d}\frac{dR}{dt}$$

giving

$$\frac{G\nu}{fd}\frac{dR}{dt} - G + C_{D_o}R^2 = 0 \quad \ldots\ldots(12)$$

Assuming that $C_{D_o} = a_o/R$, as has been shown to be approximately true over small changes of log R and accurately true when $R < 0{\cdot}5$, equation (12) becomes :

$$\frac{G\nu}{fd}\frac{dR}{dt} = G - a_o R \quad \ldots\ldots(13)$$

whence

$$\Delta t = \frac{G\nu}{fda_o}\log_e\left(\frac{G/a_o - R_1}{G/a_o - R_2}\right)$$

$$= \frac{4}{3}\frac{\sigma d^2}{\rho\nu}.\frac{1}{Ka_o}\log_e\left(\frac{G/a_o - R_1}{G/a_o - R_2}\right) \quad \ldots(14)$$

The right-hand side of equation (14) is positive only if either

$$R_1 < R_2 < G/a_o \quad \text{or} \quad R_1 > R_2 > G/a_o$$

This means that a particle with any initial velocity, either greater or less than the terminal velocity, will approach the terminal velocity given by G and Fig. 3, reaching it in infinite time.

Introducing a non-dimensional time factor

$$T_f = \Delta t\,.\,\frac{\rho\nu K}{\sigma d^2}$$

leads to the equation in dimensionless form :

$$T_f = \frac{4}{3a_o}\log_e\left(\frac{G/a_o - R_1}{G/a_o - R_2}\right) \quad \ldots\ldots(15)$$

A continuous curve of T_f as a function of R can be made by evaluating equation (15) for small ranges R_1 to R_2 and summing. The application of these graphs is discussed later.

Distance–Velocity Relationship

In this case the substitution is :

$$\frac{d^2x}{dt^2} = \frac{VdV}{dx} = \frac{R\nu^2}{d^2}\frac{dR}{dx} \quad \ldots\ldots(16)$$

Hence, with the assumption that $C_{D_o} = a_o/R$, equation (8) becomes

$$\frac{G\nu^2 R}{a_o f d^2}\frac{dR}{dx} = \frac{G}{a_o} - R \quad \ldots\ldots(17)$$

or

$$\frac{4d\sigma}{3\rho K a_o}\int_{R_1}^{R_2}\frac{RdR}{G/a_o - R} = \int_{x_1}^{x_2} dx \quad \ldots\ldots(18)$$

$$\therefore \Delta x = \frac{4d\sigma}{3\rho K a_o}\left[(R_1 - R_2) + \frac{G}{a_o}\log_e\left(\frac{G/a_o - R_1}{G/a_o - R_2}\right)\right] \ldots(19)$$

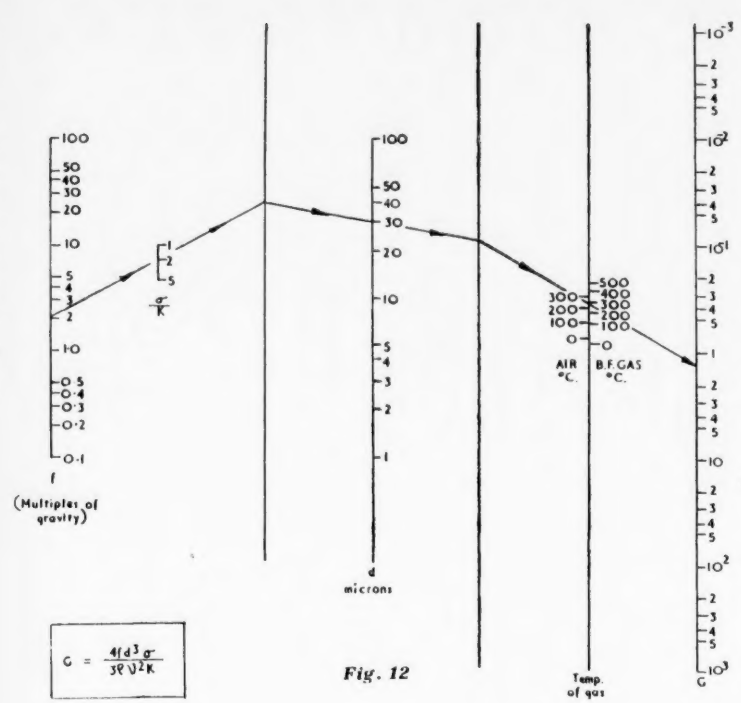


Fig. 12

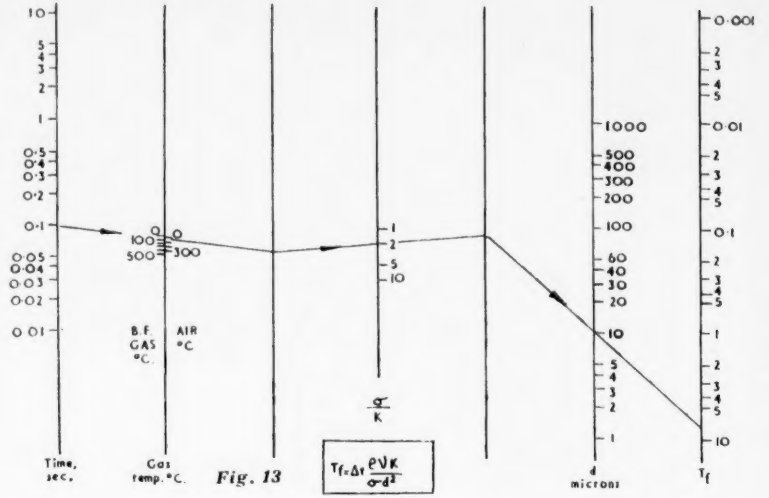


Fig. 13

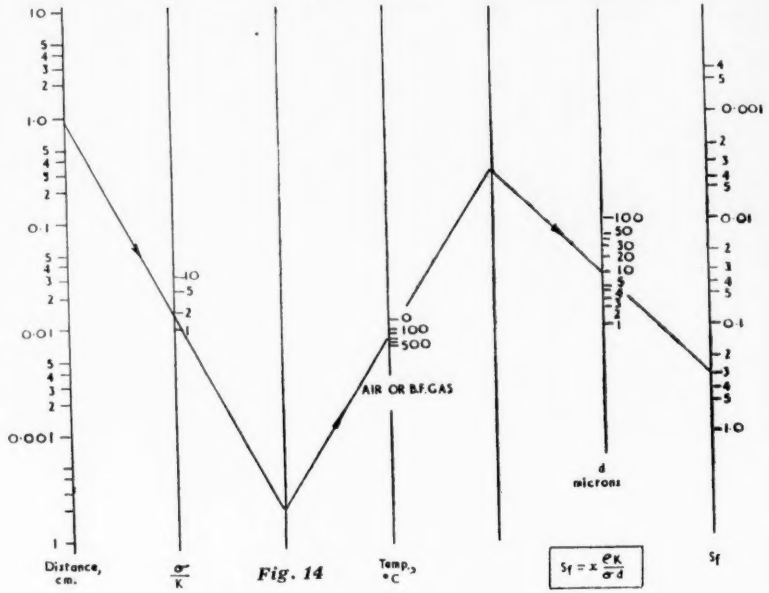


Fig. 14

Fig. 12—Nomogram for calculating G (logarithmic scales)

Fig. 13—Nomogram for calculating T_f or time (logarithmic scales)

Fig. 14—Nomogram for calculating S_f (logarithmic scales)

The conditions for Δx to be positive are the same as before in equation (14). Introducing a non-dimensional distance factor :

$$S_f = \Delta x \cdot \frac{\rho K}{\sigma d}$$

converts equation (19) to

$$S_f = \frac{4}{3a_0}\left[(R_1 - R_2) + \frac{G}{a_0}\log_e\left(\frac{G/a_0 - R_1}{G/a_0 - R_2}\right)\right] \quad \ldots(20)$$

Application of Equations to the Whole Motion

Equations (15) and (20) apply only over a restricted range R_1 to R_2. In order to calculate the motion over the whole range from zero to the terminal velocity, it is necessary to divide the motion into several stages and, having found the time and distance taken to traverse these stages, to sum them for the whole motion. This has been done and leads to Figs. 4 and 5, application of which is discussed elsewhere (p. 296).

Distance–Time Relationship

Equation (8) cannot readily be integrated directly to give a distance–time relationship. A relationship can be determined analytically, however, when the motion is according to Stokes' law and starts from an initial state of rest relative to the gas. From equations (20) and (15)

$$\frac{S_f}{G} = \frac{4}{3a_0G}(R_1 - R_2) + \frac{1}{a_0}T_f \quad \ldots\ldots\ldots\ldots(21)$$

and, since $R_1 = 0$, from equation (15) :

$$R_2\frac{a_0}{G} = 1 - \exp\left(-\frac{3}{4}a_0T_f\right)$$

so that equation (21) becomes :

$$\frac{S_f}{G} = \frac{T_f}{a_0} - \frac{4}{3a_0^2}\left\{1 - \exp\left(-\frac{3}{4}a_0T_f\right)\right\} \quad \ldots(22)$$

In the Stokes' law range, for which equation (20) applies, $a_0 = 24$, and therefore

$$432\frac{S_f}{G} = 18T_f - 1 + \exp(-18T_f) \quad \ldots\ldots\ldots(23)$$

This equation has been plotted in Fig. 6, with S_f/G and T_f as ordinate and abscissa. This curve is therefore, to some scale, the distance–time curve for any particle obeying Stokes' law. A similar series of curves, for higher values of G (so that Stokes' law does not apply throughout) made by evaluating equations (15) and (20) and plotting corresponding values, is shown in Fig. 7. The equation of that part of the motion which proceeds at the terminal velocity, *i.e.*, the linear parts of the curves in Fig. 7, may be represented as :

$$S_f = R_TT_f - b \quad \ldots\ldots\ldots\ldots(24)$$

where values of b are given below :

G	R_T	b
24	0·92	0·0418
50	1·76	0·0748
100	3·21	0·1254
200	5·63	0·1823
300	7·69	0·2182
400	9·65	0·2575
600	13·1	0·3066
1000	19·0	0·3744

APPENDIX II
NON-ACCELERATED MOTION

In this Appendix formulae and curves are derived for calculating the distance travelled and the time taken for resistance to reduce the relative velocity of particle and gas to zero or, by a simple extension, to any other given value. This is a special case of Appendix I in which the accelerating force is zero and a better approximation to the drag curve is possible, making for greater accuracy.

Basic Equation

The curves of resistance coefficient against Reynolds number cannot be used as they stand. As an approximation it is assumed that for changes in Reynolds number of 2 to 1 ratio or less, the drag curve (on logarithmic paper) may be represented by a straight line. This leads to the following formula :

$$C_D = \left(\frac{8 \times \text{resistance}}{\pi d^2\rho V^2}\right) = \frac{a}{R^n} \quad \ldots\ldots\ldots\ldots(25)$$

where a and n are constants for each range ; Stokes' law is a special case where $a = 24$, $n = 1$. Values of C_D, a, and n for spheres are given below :

Resistance of Spheres, $C_D = a/R^n$

Reynolds Number	C_{D_1}	C_{D_2}	a	n
0–0·5	∞	48·00	24·00	1·0
0·5–1·0	48·00	24·40	24·40	0·9305
1·0–2	24·40	14·00	24·40	0·8016
2–4	14·00	8·26	23·88	0·7714
4–8	8·26	5·20	20·40	0·6572
8–16	5·20	3·30	20·36	0·6562
16–32	3·30	2·22	16·70	0·5850
32–64	2·22	1·52	13·96	0·5331
64–128	1·52	1·02	13·22	0·5201
128–256	1·02	0·70	13·10	0·5181
256–512	0·70	0·52	11·34	0·4925
512–1024	0·52	0·42	4·08	0·3175

The motion of a particle with shape factor K moving initially relative to a fluid and acted on only by the fluid resistance can be derived from Newton's second law of motion as :

Mass × deceleration = resistance

i.e.:

$$\frac{4\pi\sigma}{3}\left(\frac{d}{2}\right)^3\frac{d^2x}{dt^2} = KC_D\frac{\pi}{8}\rho V^2d^2$$

or

$$\frac{d^2x}{dt^2} = \frac{3\rho KC_D}{4\sigma d}V^2 \quad \ldots\ldots\ldots\ldots(26)$$

d^2x/dt^2 may be replaced by $V.dV/dx$ or by dV/dt, depending on whether distance or time is important to the problem.

Distance–Velocity Relationship

Considering distance first, from equations (25) and (26) :

$$\frac{1}{V}\frac{dV}{dx} = \frac{3\rho K}{4\sigma d}\frac{a}{R^n} \quad \ldots\ldots\ldots\ldots(27)$$

But $R = Vd/\nu$, and hence for any one particle motion :

$$\frac{dR}{R} = \frac{dV}{V}$$

and equation (27) becomes :

$$\frac{1}{R}\frac{dR}{dx} = \frac{3\rho K}{4\sigma d}\frac{a}{R^n} \quad \ldots\ldots\ldots\ldots(28)$$

or $$\int R^{n-1}dR = \int \frac{3\rho K}{4\sigma d} \cdot a dx \quad \text{.....(29)}$$

Integrating between limits R_1 and R_2 :

$$\frac{1}{n}(R_2^n - R_1^n) = \frac{3\rho K}{4\sigma d} a(x_2 - x_1) \quad \text{.....(30)}$$

But $$\frac{R^n}{a} = \frac{1}{C_D}$$

so that :

$$(x_2 - x_1) = \frac{4\sigma d}{3\rho K n}\left(\frac{1}{C_{D_2}} - \frac{1}{C_{D_1}}\right) \quad \text{.....(31)}$$

Values of

$$\frac{4}{3n}\left(\frac{1}{C_{D_2}} - \frac{1}{C_{D_1}}\right)$$

depend solely upon the range of Reynolds numbers over which the analysis holds.

The distance to come to rest from a given velocity may be calculated by adding together the successive distances to halve the velocity (and therefore the Reynolds number). The final step is to find this distance in the Stokes' law range. From equation (30), when $a = 24$ and $n = 1$:

$$(x_2 - x_1) = \frac{\sigma d}{36\rho K} \quad \text{.....(32)}$$

is the distance to stop from $R = 0{\cdot}5$

The successive integration has been performed and the result is given in the table below and in Fig. 8. A ' distance factor ' S_f is defined as :

$$S_f = \text{distance to stop} \times \frac{\rho K}{\sigma d} \quad \text{.....(33)}$$

$$S_f = \sum_{R=R_1}^{R=0{\cdot}5}\left[\frac{4}{3n}\left(\frac{1}{C_{D_2}} - \frac{1}{C_{D_1}}\right)\right] + \frac{1}{36} \quad \text{.....(34)}$$

The distance travelled in passing from any one velocity to any other may be found from Fig. 8 by finding the S_f values for each velocity (or rather, its corresponding Reynolds number) and subtracting. Corresponding distance factors and Reynolds numbers are as follows :

Reynolds Number at Start	S_f (Distance Factor to Come to Rest)
0·5	0·0278
1	0·0556
2	0·1062
4	0·1935
8	0·3362
16	0·5612
32	0·9065
64	1·4151
128	2·1470
256	3·1969
512	4·6853
1024	6·6527

Time–Velocity Relationship

The calculation of the time–velocity relationship is similar to that of the distance–velocity relationship. Equation (27) may be rewritten :

$$\frac{dV}{dt} = \frac{3\rho K a}{4\sigma d R^n} V^2 \quad \text{.....(35)}$$

and since $$\frac{dV}{V} = \frac{dR}{R} \text{ and } R = \frac{Vd}{\nu}$$

$$\int R^{n-2}dR = \int \frac{3\rho\nu K a}{4\sigma d^2} dt \quad \text{.....(36)}$$

corresponding to equation (29).

Integrating equation (36)

$$\frac{1}{n-1}(R_2^{n-1} - R_1^{n-1}) = \frac{3\rho\nu K a}{4\sigma d^2}(t_2 - t_1) \quad \text{.....(37)}$$

and since $C_D = a/R^n$:

$$t_2 - t_1 = \frac{1}{n-1}\left(\frac{1}{C_{D_2}R_2} - \frac{1}{C_{D_1}R_1}\right)\frac{4\sigma d^2}{3\rho\nu K} \quad \text{.....(38)}$$

In the Stokes' law range $n = 1$, and therefore equation (36) cannot be integrated in the form of equation (37). Accordingly, values of a time factor T_f have been calculated where :

Time to reduce velocity to that corresponding to $R = 0{\cdot}5$

$$= T_f \frac{\sigma d^2}{\rho\nu K} \quad \text{.....(39)}$$

where $$Tf = \sum_{R}^{R=0{\cdot}5} \frac{4}{3(n-1)}\left(\frac{1}{C_{D_2}R_2} - \frac{1}{C_{D_1}R_1}\right) \quad \text{.....(40)}$$

Values of T_f are given in Fig. 9 and below :

Reynolds Number at Start	T_f (Time Factor to Reach Reynolds Number = 0·5)
1	0·03700
2	0·07430
4	0·10609
8	0·13030
16	0·15008
32	0·16571
64	0·17655
128	0·18383
256	0·18959
512	0·19438
1024	0·19717

In the range ($R < 0{\cdot}5$) in which Stokes' law holds with sufficient approximation, equation (36) must be integrated as :

$$\text{Log}_e \frac{R_2}{R_1} = 18 \frac{\rho\nu K}{\sigma d^2}(t_2 - t_1) \quad \text{.....(41)}$$

Clearly the time taken to come to rest, relative to the gas, is infinite, but the time taken to reach any definite small velocity may be calculated. For the sake of generality a factor T'_f is introduced, where :

$$T'_f = \frac{1}{18}\log_e \frac{0{\cdot}5}{R} = \frac{\rho\nu K}{\sigma d^2} \times (\text{time to reduce from } R = 0{\cdot}5 \text{ to } R) \quad \text{.....(42)}$$

Values of T'_f are plotted in Fig. 9 as a continuation of the curve of T_f against R. The time taken to reduce the Reynolds number from one value to another is derived from this curve by subtracting algebraically the ordinates corresponding to the two Reynolds numbers, and multiplying by $d^2\sigma/\rho\nu K$.

APPENDIX III

Determination of Terminal Velocity

The formulae and curves of Appendices I and II have been derived with the tacit assumption that the shape and diameter of a particle are known. This is not usually the case. A common method of determining the effective sizes of particles is by settlement or by elutriation. In each of these methods the decisive quantity is the free falling speed under the action of gravity; in the elutriation method this speed is balanced against that of an upward moving stream of air.

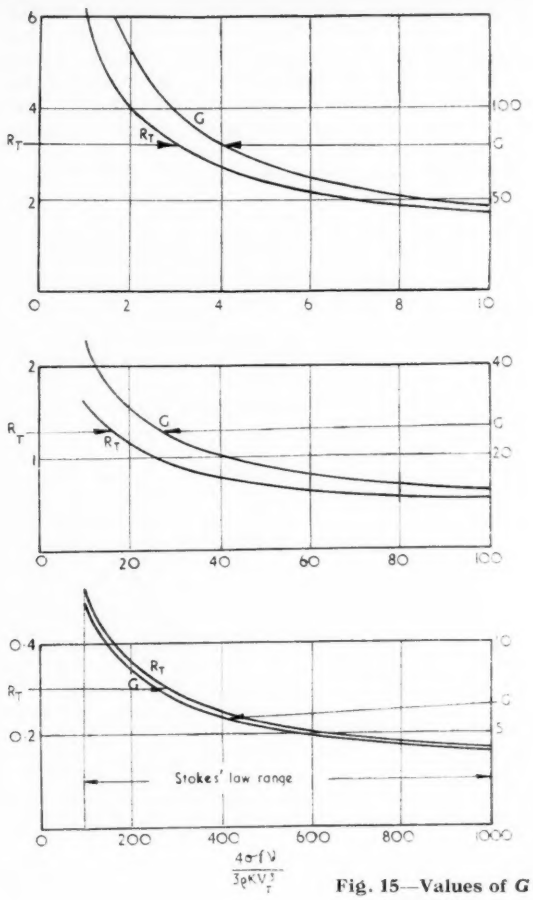


Fig. 15—Values of G and R_T corresponding to various free falling speeds represented by $4\sigma f v / 3\rho K V_T^2$

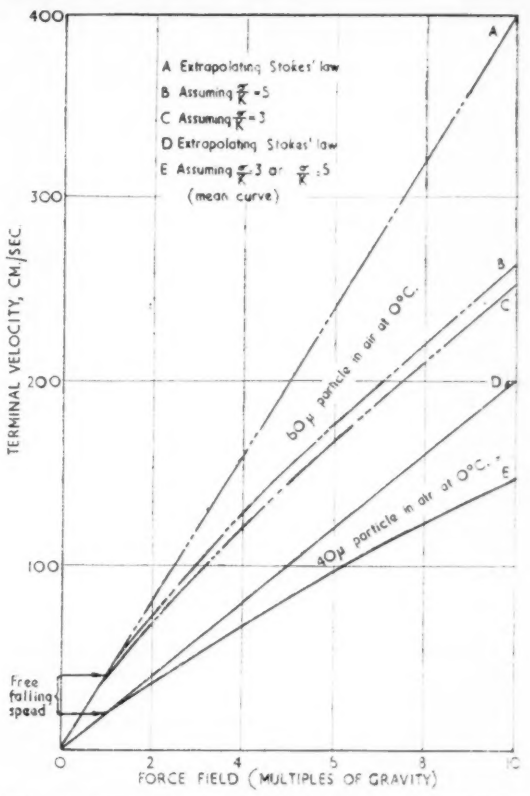

A Extrapolating Stokes' law
B Assuming f/K = 5
C Assuming f/K = 3
D Extrapolating Stokes' law
E Assuming f/K = 3 or f/K = 5
(mean curve)
60μ particle in air at 0°C.
40μ particle in air at 0°C.
Free falling speed
TERMINAL VELOCITY, CM./SEC.
FORCE FIELD (MULTIPLES OF GRAVITY)

Fig. 17—Examples to show the effect of extrapolating Stokes' law to give terminal velocity under different conditions of acceleration, compared with method given in Fig. 16.

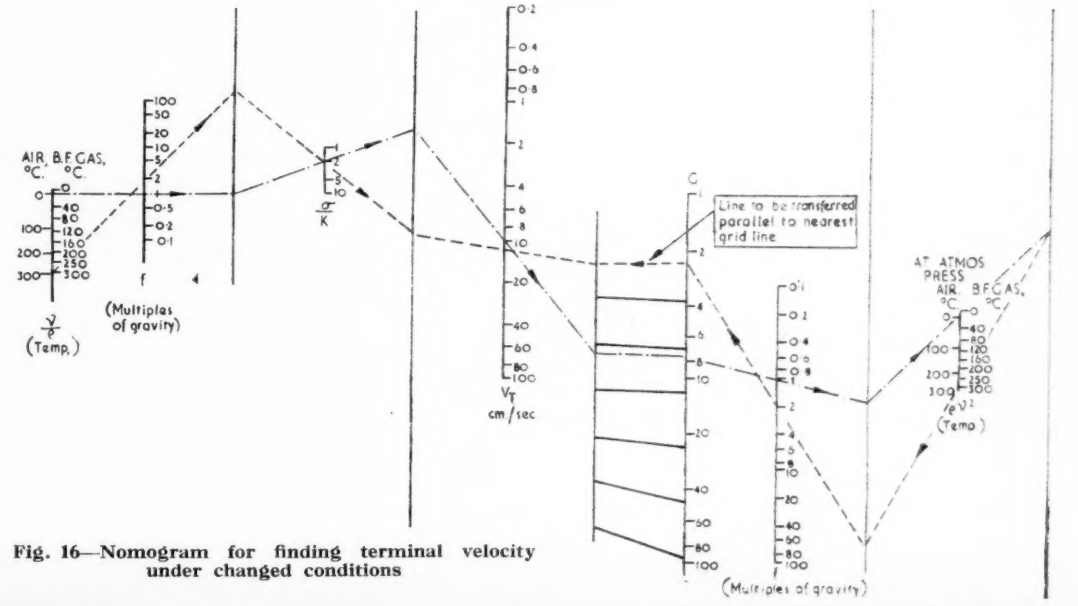


Fig. 16—Nomogram for finding terminal velocity under changed conditions

Although free falling speed is more nearly related to the problem of particle motion than is, for example, particle diameter, it is not possible to deduce from it, immediately, the terminal velocity under a force other than gravity. It has been general to assume that Stokes' law holds and that, therefore, terminal velocity is proportional to accelerating force. Whilst it is true that Stokes' law holds during elutriation of a particle of about 40-μ dia. or less, it certainly does not hold when the accelerating force is several times that of gravity. This Appendix gives a method of determining the terminal velocity which is related to the actual drag curve, does not assume an extrapolation of Stokes' law, and can take into account the effect of changing the gas.

Analysis

Terminal velocity is related to Reynolds number by :

$$V_T = R_T \frac{\nu}{d} \quad \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots (43)$$

where d is the equivalent spherical diameter. Also R_T is a function of G, where :

$$G = \frac{4fd^3\sigma}{3\rho\nu^2K} \quad \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots (44)$$

In any particular case, ρ, ν, f are known with sufficient accuracy, and σ and K are not. For the present, it is assumed that a fair guess may be made of their values, and it will be shown by examples that the error involved if the ratio σ/K is over- or under-estimated by 50%, is not serious. Eliminating d between equations (43) and (44), leads to :

$$G = \frac{4\sigma f\nu}{3\rho K V_T{}^3} R_T{}^3 \ldots\ldots\ldots\ldots\ldots\ldots\ldots (45)$$

It is also known that G is a function of R_T, independently of equation (45), and hence the solution of these simultaneous relationships leads to a value of G corresponding to conditions at the elutriation. This solution has been obtained graphically and, in Fig. 15, has been plotted showing values of G and of R_T corresponding to values of the function :

$$H = \frac{4\sigma f\nu}{3\rho K V_T{}^3} \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots (46)$$

The function H can be calculated immediately from the details of the elutriation, assuming a value of σ/K. Once a value of G is known (from H and Fig. 15), it is possible to calculate a new value corresponding to the changed accelerating force, gas viscosity, and gas density, and from this, reversing the steps of the calculation, the terminal velocity may be determined. The calculation may be made entirely on the nomogram (Fig. 16).

To illustrate the errors involved in extrapolating Stokes' law and in assuming an incorrect value for σ/K, a series of curves is plotted in Fig. 17. It can be seen that the present method affords considerable improvement, especially when the accelerating force is high.

APPENDIX IV

PHYSICAL PROPERTIES OF BLAST-FURNACE GAS AND AIR

This paper is concerned with two gases : Blast-furnace gas is the subject of the calculations, and air is the medium used in the elutriation of dust samples. The viscosity and density of these gases occur frequently, both separately and in combination, and it is convenient to express these combinations in the nomograms by the temperature and pressure of the gas. Thus, viscosity and density do not appear as such in the nomograms.

In order that this may be done there must be some confidence that the mean properties chosen are representative of the blast-furnace gases of varied compositions that are made. The main constituents of this gas are :

CO 25–32%, CO_2 8–15%, H_2 0–4%, N_2 56–61%

Thus, density may be expected to vary by ± 3% at the most, and viscosity (which is substantially affected only by the variation in hydrogen content) by ± 1%.

Mean values of viscosity and density at various temperatures for blast-furnace gas and air are given in Table I and are assumed in the nomograms. The values for blast-furnace gas are taken from British Standard Code 1042 (1943) " Flow Measurement."

Table I

VISCOSITY AND DENSITY OF AIR AND BLAST-FURNACE GAS

Temp., °C.	Absolute Viscosity, Poises (η)	Kinematic Viscosity, Stokes (ν)	Density at Atmos. Press.,* g./c.c. (ρ)	$\rho_t{}^2$ at Atmos. Press.,† g.cm./sec./sec.
	× 10^{-4}		× 10^{-3}	× 10^{-4}
Blast-furnace gas				
0	1·599	0·123	1·3	0·1967
20	1·708	0·141	1·211	0·2407
40	1·803	0·159	1·134	0·2867
80	1·980	0·197	1·005	0·3900
120	2·149	0·238	0·903	0·5115
160	2·311	0·282	0·820	0·6517
200	2·461	0·328	0·750	0·8072
250	2·647	0·390	0·679	1·0322
300	2·818	0·455	0·619	1·2823
400	3·137	0·595	0·527	1·8668
500	3·439	0·749	0·459	2·5755
Air at atmospheric pressure (760 mm. Hg.)				
0	1·70	0·131	1·293	0·223
100	2·18	0·230	0·946	0·501
200	2·58	0·346	0·746	0·893
300	2·95	0·479	0·616	1·413

* Add 5% at 20 in. W.G. † Subtract 5% at 20 in. W.G.

Forthcoming Publications

The April issue of the *Journal* will contain the following B.I.S.R.A. papers :

" *The Traffic of Iron and Steel Works : The Application of Very High-Frequency Radio Communication*," by H. H. Mardon, H. A. Chapman, and M. D. Brisby.

" *A Proposed Method of Specifying Travel Motion Performance for Overhead Cranes*," by D. A. Wise and L. N. Bramley.

" *Characteristics of Scrap in Relation to Bulk Handling*," by E. L. Diamond.

Blast-Furnace Gas Cleaning

AN ANALYSIS OF PLANT PERFORMANCE

By R. F. Jennings, B.Sc.

SYNOPSIS

The operation of cleaning plant for blast-furnace gas is surveyed from an engineering standpoint. A very clean gas can be obtained by present-day systems of cleaning, but the consumption of mechanical power and water, the loss of gas pressure, and the large space requirements involved, might well be substantially reduced.

Principles of operation are described and discussed, and possible lines of improving separating power and mechanical efficiency are suggested.

Attention is drawn to the lack of data on this subject, particularly on water-spray systems and the behaviour and nature of blast-furnace dust.

No attempt is made to assess detailed costs of gas-cleaning systems.

APPROXIMATELY 1,500,000 $\times 10^6$ cu. ft. of blast-furnace gas, representing about 1500 $\times 10^6$ therms, are generated each year from the blast-furnaces in Great Britain. The value of the gas, as fuel, is seriously impaired by the presence of suspended solid and liquid impurities, and in the past the first aim has been the removal of most of these impurities. Today, there is no technical barrier to the complete cleaning of blast-furnace gas, and cleaning plants are in operation which are capable of removing all particles greater than 5μ* in effective diameter, the cleaned gas having a dust content as low as 0·002 grain/cu. ft.

Complete cleaning, however, is costly ; it is estimated that at least one-tenth of the value of the gas is spent on the removal of impurities, so that the total cost of cleaning in the iron and steel industry is of the order of £1,250,000 per year. A high proportion of this cost arises from the consumption of energy, which can be debited from the total thermal value of the gas.

The development of gas-cleaning equipment has been largely empirical and hence not always in full accord with the principles of particle separation from a gas stream. There is very little published information on the functions and limitations of the various methods of cleaning, in terms of the principles of operation and the behaviour of particular suspensions. Little is known of the influence of dust concentration, chemical purities, particle size, density, and surface properties, upon the performance of gas-cleaning systems. The advent of such knowledge should bring considerable improvements in design.

As a first step, gas-cleaning methods and plant performance are examined, and the principles of particle separation are expressed in terms which may be applied by the designer and the user of plant. An analysis of the operation of various items of plant is related to the problems of economic gas cleaning, and possible improvements are suggested.

Gas cooling is excluded, since its function is primarily to reduce the initial moisture content of the gas, as distinct from gas cleaning, and it operates under laws other than those of the removal of suspended solid matter. Nevertheless, it is recognized that many wet-cleaning plants and scrubbing towers serve the dual purpose of gas cleaning and cooling.

* $1\mu = 10^{-4}$ cm.

IMPURITIES IN BLAST-FURNACE GAS

The gas emerging from the top of the burden in a blast-furnace contains large numbers of particles of non-gaseous matter. This material is either useless or harmful, and as far as possible, the particles which may cause a nuisance in the subsequent use of the gas must be removed.

In the past, gas engines have usually required the cleanest gas, less than 0·01 grain/cu. ft., but severe fluxing of refractories in coke ovens and stoves may also be caused by the prolonged use of gas of such a dust content. Higher concentrations may inhibit the combustion of the gas, as well as clogging the mains, valves, and other points of constriction in the gas system. It is important to emphasize these rather obvious points since the degree of improvement in the economic or technical value of the cleaned gas should be a useful criterion for assessing the economic performance of the cleaning plant.

The objectionable matter in blast-furnace gas is mostly solid. Liquid impurities cause little or no nuisance to the combustion of the gas, except by reducing its net calorific value ; however, they may be slightly corrosive, and if deposited undiluted, with dust, may give rise to accumulations of hard scale.

The concentration of solid particles in the top gas varies widely in weight per unit volume with the furnace practice and the kind of burden. Quantities ranging from 10 to 40 grains/cu. ft. are quoted for different blast-furnace plants, but these figures may be misleading since a large proportion of the total weight of dust at the furnace top consists of the largest sizes of particles, the removal of which presents little difficulty. The major problem is the removal of the finest particles in low concentration, *e.g.*, less than 5μ dia. at 0·05 grain/cu. ft.

It is common practice to express the efficiency of cleaning plants in the form of the ratio

$$\frac{\text{Weight of solid matter removed}}{\text{Original total solid content of gas}}$$

Paper PE/A/29/49 of the Plant Engineering Division of the British Iron and Steel Research Association, received 19th September, 1949. Mr. Jennings is Technical Secretary of the Plant Engineering Division of B.I.S.R.A.

This may be misleading, since it takes no account of the individual size or other physical characteristics of the dust particles and gives no indication of their numbers and distribution in the gas stream. There is little published information on the characteristics of typical furnace dusts, but the following general features are likely to be met in all cases and will influence the behaviour of dust suspensions in any gas-cleaning plant :

(1) The particles cover a wide range of size from above 2000μ down to the finest sub-microscopic sizes (which will be fully under the influence of Brownian movements).

(2) The proportion, by weight, of the various sizes of particles will vary considerably ; the larger particles constitute the major bulk of the dust in top gas, but the weight of very fine particles may sometimes be quite high.

(3) The dust contains a large number of chemically different materials, with a wide range of densities. This chemical heterogeneity occurs throughout all sizes of dust, as is shown by the following chemical analysis of superfine particles, sampled by Messrs. Stewarts and Lloyds, Ltd., of Corby, from a clean-gas main at a concentration below 0·008 grain/cu. ft. :

	%		%
Fe_2O_3	25·6	CuO	3·4
SiO_2	19·3	ZnO	4·5
Al_2O_3	10·0	MnO	7·9
CaO	7·1	C	9·6
MgO	1·1	S	2·2

For comparison, typical analyses of top-gas dust, as given by O. R. Rice,[1] are as follows :

	Example 1	Example 2
Fe,* %	44	34
Oxides,† %	18	30
C, %	10	10
Alkalis, %	4	5
Acids, %	5	6

* Calculated as $FeO_{1\cdot4}$
† $Al_2O_3 + SiO_2 + CaO + MgO$

(4) Most of the particles are likely to be substantially unchanged from the original dust in the burden as it entered the furance. Some of the finer particles, particularly the alkali metal compounds, however, are produced from the volatile fume in the furnace and may thus possess unusual surface characteristics.

GAS-CLEANING PLANT

Arrangement of Plant

Complete cleaning plants are arranged as a number of different processes in series. Each mechanism is designed to remove a range of dust concentration which escaped separation in the preceding mechanism and also to act as a partial size selector. Thus, in its passage through a multiple cleaning system, the dust becomes composed of smaller and smaller particles at lower and lower concentrations. To deal with this reduction in size and concentration different mechanisms for separation are employed in successive stages of the cleaning.

The selection of the work to be done in each stage is one of the fundamental points to be decided in the design of a complete cleaning plant. Three variables which can have a considerable influence are (i) the temperature of the top gas, (ii) the degree of water-vapour saturation, and (iii) the chemical composition of the dust (particularly substances which can form a hard agglomerate when wetted). Many difficulties in plant operation arise when the moisture in contact with the dust is sufficient to wet it but insufficient to wash it away as a suspension. This trouble is likely to occur around the dew-point of the gases ; a change-over from dry to wet cleaning should therefore coincide with a sudden fall in temperature, at least on the surfaces of the cleaner, to well below the dew-point. This is not always appreciated in the design of scrubbers. On changing from wet to dry cleaning the temperature should be raised to well above the dew-point.

Performance

The exact performance of any particular cleaner cannot be guaranteed ; unknown factors always exist which cannot be measured and which cause performance to vary widely at different works. Some

Table I

SUMMARY OF PERFORMANCE DATA

Normal operating conditions : full gas rate

Type of Cleaner (Names of Common Makers)	Concentration, grains/cu. ft.		Probable Size of Particle not Separated, μ	Energy, consumption/1000 cu. ft. of gas				Number of Installations Reported,* and Remarks
				Pressure Drop, in. W.G.	Water		Motor Power, h.p./hr.	
	Inlet	Outlet			Vol., gal.	Pressure, lb./sq. in.		
Dry Cleaners :								
Dust catchers	Max.	0·1–2·0	150	0·5	..	..	..	Used at almost all plants but form of cleaner varies
Cyclones	Max.	0·5–1·0	100–40, according to size of unit	1·0, and upwards	..	..	..	
Bag filters (Halberg, Dracco)	1·0	0·008	According to pressure drop	6–12	..	..	..	13; performance varies according to material of filter bags
Wet Cleaners								
Wet scrubbers (McKee, Peabody)	5·0	0·25	25	6–9	7–10	30 about	..	13; water only has to be pumped to top of tower
Spray towers (Peabody, Whessoe)	5·0	0·125	20	1–2	20–40	100–200	..	Usually classified with scrubbers
Disintegrators (Theissen, Zchocke)	0·5	0·005	10	2–4*	10–15	Small	0·3	20; pressure rise of 2–4 in. W.G. through disintegrators*
Elect. precipitators (Lodge, Elga, R. Corp.)	0·5	0·003	5	1·0	5–10	Small	..	7; some dry. Electric consumption small

* Survey of Blast-Furnace Practice, Iron Making Division, B.I.S.R.A., 1948.

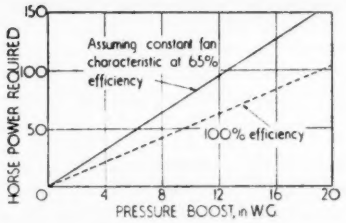


Fig. 1—Typical power requirements to boost 2,000,000 cu. ft. of gas per hour (collected from published data[9])

general figures, however, based on a designed rate of gas throughput, are indicated in Table I.

Effect of High Top-Pressure Operation

The use of increased top pressure at the blast-furnace and a corresponding increase in the rate of ironmaking with a given volume of gas, is likely to affect the design and choice of cleaning plant. Any considerable increase in the available pressure energy in the gas will have to be dissipated before the gas can be stored in gas-holders and is available for use in cleaning plant.

This excess of pressure opens up many possibilities for improved primary cleaning.

Cost of Gas Cleaning

For details of costs, reference should be made to the figures presented by Wright and Fowler,[2] for two modern plants; these authors gave figures of 0·22 and 0·14 pence per 1000 cu. ft. of gas for disintegrators and electrostatic plants, respectively, excluding capital charges. A large proportion of these costs is due to energy consumption, and may arise from two main sources:

(*a*) Boosting the gas to recover the pressure energy expended in dust separation
(*b*) The use of large quantities of spray water at high pressure.

With regard to (*a*), Fig. 1 indicates typical horse-powers required for boosting 2 million cu. ft./hr. It has therefore to be decided how much boosting

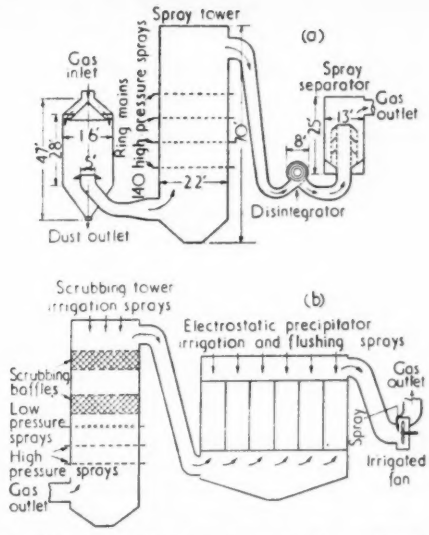


Fig. 2—Diagrammatic arrangement of cleaning plants: (*a*) plant *A*, and (*b*) plant *B*

can be economically allowed in each stage of the plant, bearing in mind the initial pressure available in the gas. This applies particularly to disintegrators.

Referring to (*b*), high-pressure sprays require considerable pumping power, and it is common practice to use 40 gal. at 100–200 lb./sq. in. per 1000 cu. ft. of gas. To raise 1000 gal. of water per hour to 200 lb./sq. in. requires at least 3 h.p., and the subsidiary power for water recirculation in and around a wet cleaner may well double this figure.

These considerations should influence the choice of cleaner, since performance is often directly related to resistance to gas flow or to the volume of spray water.

DATA FROM WORKS TRIALS

Unusually complete performance trials on gas-cleaning plant were carried out at two steelworks in 1945, at plant *A* with disintegrators and at plant *B*

Table II

PLANT *A*: PERFORMANCE ACCORDING TO SIZE OF DUST PARTICLES

Size Range of Dust, μ	Dust Catchers		Spray Tower		Disintegrator		Cleaned Gas*
	Inlet*	Effic^y, %	Inlet*	Effic^y, %	Inlet*	Effic^y, %	
>699	0·040	100	...	...	...	...	...
699–100	8·000	100	...	...	...	...	...
100–90	0·505	100	...	...	...	...	...
90–80	0·700	68	0·222	100	...	...	...
80–70	0·785	58	0·329	97	0·0080	100	...
70–60	0·423	59	0·175	96	0·0087	100	...
60–50	0·351	67	0·111	94	0·0065	100	...
50–40	0·712	56	0·311	97	0·0096	100	...
40–30	1·250	56	0·540	98	0·0087	100	...
30–20	1·820	59	0·740	98	0·0152	100	...
20–10	1·890	40	1·140	96	0·0405	92	0·0031
<10	1·970	25	1·490	88	0·1850	98	0·0030
Totals	18·446	Av. 72·6	5·058	Av. 94·4	0·2813	Av. 98·0	0·0061

* Dust concentration, grains/cu. ft.

with irrigated electrostatic precipitators. Performance data for the two plants are summarized below :

Plant A : *Gas Rate* 3,075,000 *cu. ft./hr. at N.T.P.*

	Dust Content at Inlet (grains/cu.ft.)	Efficiency %
Dust catchers	18·45	72·6
Spray tower	5·06	94·4
Disintegrator	0·281	98·0

Cleaned gas = 0·0061 grain/cu. ft.
Overall efficiency = 99·96%

Plant B : *Gas Rate* 1,980,000 *cu. ft./hr. at N.T.P.*

	Dust Content at Inlet (grains/cu.ft.)	Efficiency %
Scrubbing tower	2·006	39·0
Electrostatic precipitator	1·223	99·3
Irrigated fan	0·0082	67·0

Cleaned gas = 0·0027 grain/cu. ft.
Overall efficiency = 99·81%

Plant *A*

A diagrammatic arrangement of plant *A* is shown in Fig. 2*a*. The dirty gas enters two catchers from the furnace downcomer and travels *via* a common ring main to a single spray tower and disintegrator ; the spray separator does not act as a further cleaner. The total water flow is 46 gal./1000 cu. ft. of gas, of which 40 gal./1000 cu. ft. at 76 lb./sq. in. (pump outlet) flow to the spray-tower jet, and 6 gal./1000 cu. ft. at low pressure to the disintegrator.

Performance data in terms of size of dust particles are presented in Table II.

Plant *B*

A diagrammatic arrangement of plant *B* is shown in Fig. 2*b*. No effective dust catchers are used, the gas entering the scrubbing tower substantially as received from the furnace downcomers. Water-jet sprays and irrigated baffles are used in the scrubbing tower. The precipitator is also irrigated with sprays between the banks, and each is flushed down every 4 hr. Water flow to tower irrigation is 23 gal./1000 cu. ft. and to jet sprays 4·5 gal./1000 cu. ft. ; irrigation of precipitator takes 0·6 gal./1000 cu. ft. and flushing 0·6 gal./1000 cu. ft. ; fan irrigation takes 0·3 gal./1000 cu. ft., giving a total of 29 gal./1000 cu. ft. of gas. Performance data in terms of size of dust particles are given in Table III.

Discussion

As the initial dust concentrations in the two plants are very different, the tests are not strictly comparable ; nevertheless Tables II and III provide an unusually detailed analysis of performance.

The dust catchers at plant *A* show no indication of a ' cut-size ' below which it might be expected that almost no dust is separated ; the performance falls off below 90-μ particles and remains between 60 and 70% separation down to 20μ. This may be accounted for by the mixed semi-cyclone form of dust catcher (*see* p. 311).

The spray tower in plant *A* and the scrubbing tower in plant *B* give widely different performances. The spray tower is strikingly more efficient at all concentrations and for all size ranges. This is probably due to the use of much larger volumes of water at high pressure (*see* p. 317).

The fine cleaners at both plants are very effective, although the precipitator at plant *B* has a rather heavier dust loading. The action of the irrigated fan at this plant is also interesting. The disintegrator appears to be 100% effective down to 20μ, but the precipitator seems to be 99% effective for all sizes of particle. The small differences probably arise from the nature of the action of the two cleaners (*see* the sections on design.)

Similar examination of the performance of other gas-cleaning plants in the industry has not been possible owing to the lack of information on particle size.

PRINCIPLES OF PARTICLE SEPARATION

Before examining the construction and operation of various forms of cleaner it may be useful to consider

Table III

PLANT *B* : PERFORMANCE ACCORDING TO SIZE OF DUST PARTICLES

Size Range of Dust, μ	Scrubbing Tower		Electrostatic Precipitator		Irrigated Fan		Cleaned Gas*
	Inlet*	Effic^y, %	Inlet*	Effic^y, %	Inlet*	Effic^y, %	
>251	0·010				...	...	...
251–211	0·0254				...	...	...
211–178	0·0326				...	...	...
178–151	0·0205	30	0·1740	100	...	...	...
151–124	0·0395				...	...	...
124–104	0·0645				...	...	...
104–89	0·058				...	...	...
89–76	0·0423	52	0·0198	99·8	0·00022	100·0	...
76–60	0·0708	35	0·0459	99·0	0·00043	98·6	0·000006
60–50	0·0810	30	0·0557	99·3	0·00041	97·6	0·000012
50–40	0·130	33	0·0830	99·4	0·00054	86·0	0·000075
40–30	0·220	28	0·1540	99·5	0·00076	74·0	0·000198
30–20	0·312	38	0·1910	99·5	0·00100	60·0	0·000397
20–10	0·400	40	0·2400	99·4	0·00135	48·0	0·000707
<10	0·5000	48	0·2600	96·5	0·00350	62·0	0·001370
Totals	2·0066	Av. 39·0	1·2234	Av. 99·3	0·00822	Av. 67·0	0·002765

* Dust concentration, grains/cu. ft.

the physical principles governing the separation of dust particles from a gas. By the law of conservation of energy this separation requires the application of a specific amount of force. This can be supplied externally or it may be provided from the pressure or velocity energy of the gas itself.

An illusion that the particles possess their own energy for separation is presented by gravitational attraction which is always acting as a separating force. This point is often overlooked when the mechanism of gas cleaning is being examined.

The force exerted on any particle can be expressed as the product of the mass of the particle and the acceleration imparted to it, relative to that of the surrounding gas stream. Similarly the work done in separating the particle a finite distance is the product of force and distance moved. The true efficiency in terms of work of any mechanism for separating particles from a gas stream is therefore :

$$\frac{\text{Sum of useful work done on the particles}}{\text{Total work put into the machine}}$$

This expression takes no account of the time factor ; all practical assessments of operating efficiency must be based on separating efficiency per unit time.

Forces Available for Separation

Whatever the source, the forces may be applied in one or more of the following ways : (*a*) As restraining forces, (*b*) as accelerating forces, or (*c*) as momentum forces.

Restraining Forces—Sieves or filters theoretically can be made to perform the almost complete separation of dust from a gas by restraining the passage of the individual particles. The behaviour of such filters, however, is not readily predictable, since it is usual to establish a layer of the dust particles themselves to act as a restraining screen on the surface of the filter. The pressure drop through this screen therefore varies according to the dust load lying upon it. The practical problems associated with the filtration of hot saturated gas are discussed in a later section.

Accelerating Forces—The force of separation most frequently takes the form of an acceleration imposed directly on the dust particle relative to the surrounding gas stream. Common examples of the sources of such a force are gravitational attraction, centrifugal repulsion, or electrostatic attraction. Under such a force the general equation of motions of a particle can be expressed as :

$$\text{Force on particle} = (\text{mass of particle}) \times (\text{its acceleration}) \mp (\text{drag on particle})$$

Stringer[3] has shown that this equation of motion can be integrated, at least approximately, in respect of time and distance over a wide range of Reynolds numbers of flow. In presenting the results of this integration he has used a group of non-dimensional parameters to describe the motion in general terms so that the conditions can be applied to a wide range of particles and forces.

Momentum Forces—Under certain conditions the mechanism of dust-particle separation may act through a sudden change in the momentum energy of the particle. The energy is opposed by the drag of the gas stream and the particle is thus decelerated until it comes to rest relative to the stream. The equation of motion has been approximately integrated by Stringer,[3] and he has expressed the results in terms of three non-dimensional parameters having the same form as for the case of accelerated motion.

Thus, if an initial Reynolds number of flow relative to the gas stream is known for a given parameter of either time or distance, then the corresponding time or distance to reduce the flow to a given lower velocity can be read off from the tables and graphs given by Stringer. It will be noted that the distance parameter is evaluated to zero, but the time parameter tends to infinity as the Reynolds number falls towards zero.

The use of this information to examine the motion of particles in various items of plant is discussed in later sections.

DESIGN AND OPERATION OF PLANT

The design and operation of various forms of cleaner are discussed in the following sections ; the analysis of particle motion is applied where practicable, and possible lines for the improvement of plant are suggested.

The following conditions are assumed in the discussion :

(1) The 'effective diameter' of the dust particles (*d*) is equal to the diameter of the sphere of equivalent mass and density

(2) The effective density of the dust particles is assumed to be either 4·5 g. ml. (iron ore), 2·6 g./ml. (limestone, silica, etc.), or 1·0 g. ml. (coke)

(3) The density of the gas is constant, $\rho = 0{\cdot}0012$ g./ml.

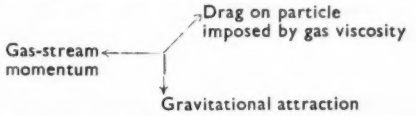
(4) The absolute viscosity of the gas is constant, $\eta = 170 \times 10^{-6}$ poises ; therefore the kinematic viscosity, $\nu = 0{\cdot}14$ stokes

(5) The gas-velocity changes due to changes in temperature and pressure in a particular section of each cleaner are ignored.

DRY CLEANING SYSTEMS

Settling Chambers

The simplest form of cleaner is the settling chamber, in which the dust particles separate from the gas stream under the action of gravity. The forces acting in a settling chamber are as follows :



As a maximum distance of separation between the dust particles and the gas stream is required, the dust particles should remain in the chamber for a maximum time. To permit this, and at the same time to ensure that the movement of the dust particles under gravity does not cause re-entrainment of the particles into the gas stream, the gas flow should be horizontal, *i.e.*, normal to the direction of gravity.

To increase further the time during which the gas stream remains in the chamber, the gas velocity should be reduced by enlarging the cross-section of the chamber. For example, a settling chamber to pass 2,000,000 cu. ft. of gas per hour might be 20 ft. long, of square cross-section, and 10 ft. high (Fig. 3). The gas would then pass the length of the chamber in about 3·5 sec.

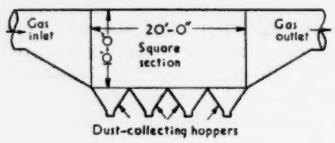


Fig. 3 — Simple settling chamber

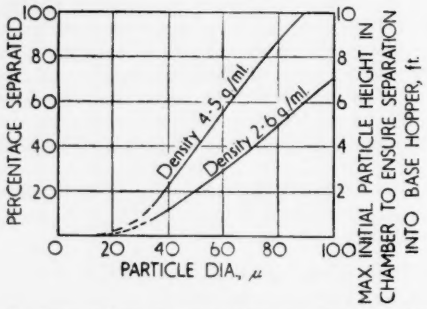

PERCENTAGE SEPARATED
PARTICLE DIA., μ
Density 4·5 g/ml
Density 2·6 g/ml
MAX. INITIAL PARTICLE HEIGHT IN CHAMBER TO ENSURE SEPARATION INTO BASE HOPPER, ft.

Fig. 4—Separation of particles of various sizes in settling chamber 10 ft. high

The ability of a given particle to separate from the gas stream in this time will depend upon its initial position in the chamber and its rate of settling. If all the dust can be assumed to be evenly distributed across the incoming-gas main, then the percentages of the various sizes of particles which may separate into the collecting hoppers can be determined.

It is seen from Fig. 4 that, even in a large settling chamber, with a transit time of 3·5 sec., only particles greater than 80μ at 4·5 density will separate completely. For lighter particles, of 2·6 density, only 70% of those of 100μ will be removed. To separate completely the finer particles the total height of settling would have to be reduced; this would require the settling chamber to be divided horizontally by a number of shelves, each capable of receiving the dust, and also to have a mechanism for removing the dust layers. Further, many constructional problems would be involved, and the probability of turbulent eddies disturbing the free settling of the particles would be increased.

The simple settling chamber has therefore been abandoned in favour of other systems in which the forces of separation can be intensified and concentrated.

Momentum Dust Catchers

The momentum energy of the dust particles flowing in the gas stream can provide a considerable separating force, particularly if added to gravitational acceleration. This is achieved in a number of dust-catcher designs at present used for gas cleaning.

The most effective arrangement of this type is shown in Fig. 5. The gas enters the downward-opening funnel (preferably having an included angle of about 12°), and its velocity is decreased in the expansion of the funnel. The dust particles are therefore accelerated, relative to the gas stream, by their

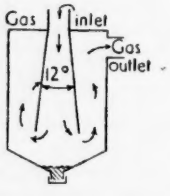
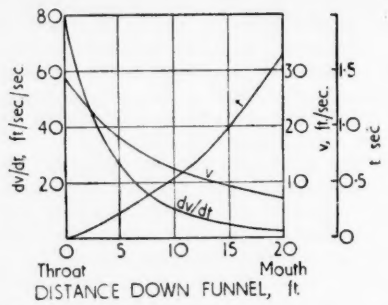


Fig. 5—Momentum dust catcher



dV/dt = deceleration of gas stream
V = velocity of gas stream
t = time (approx.) for gas stream to travel down funnel

Fig. 6—Kinetic changes in gas stream in expanding funnel (calculated)

initial momentum as well as by gravitational attraction. It has been claimed that this form of dust catcher can remove 85% of the dust in the raw gas from the furnace. However, such a figure has little meaning unless the size and density of the particles are taken into account.

Principles of Operation

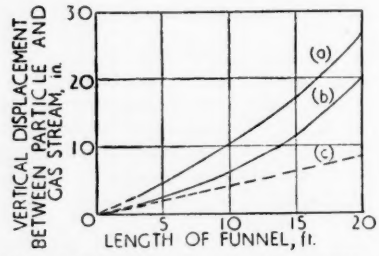
To assess the value of the momentum dust catcher it is necessary to consider its mode of operation in detail. The momentum of the particles due to the deceleration of the gas stream will decrease rapidly as the funnel expands, as it may be shown that the deceleration of the gas stream is :

$$V\left(\frac{dV}{ds}\right) = -\frac{2Q^2}{\pi^2 R^5}\tan\frac{\theta}{2}$$

where Q is the volume of gas passing in unit time, R is the radius of funnel at any point, and θ is the included angle of the funnel.

As an example, the instantaneous deceleration, the velocity, and time of travel of a gas steam of 2,000,000 cu. ft./hr. passing down a vertical funnel 20 ft. long and expanding from 5 ft. to 10 ft. in dia., have been calculated to be as shown in Fig. 6.

Now f in the vertical funnel is the combined gravitational attraction (g) and the instantaneous gas-stream deceleration (dV/dt). At the funnel throat, $f = 32{\cdot}4 + 79{\cdot}5 = 111{\cdot}9$ ft./sec./sec., while at the mouth it is reduced to 34·8 ft./sec./sec., or only slightly more than gravity alone. The values of f for successive distance intervals down the funnel can thus be determined by reference to Fig. 6. From these values it



(a) 50-μ particle under gravity and gas deceleration
(b) 50-μ particle under gravity only
(c) 50-μ particle under gravity only, in straight, vertical pipe

Fig. 7—Separating influences on 50-μ particle in vertical gas stream in expanding funnel (calculated)

can also be shown that a 50-μ particle will move at its terminal velocity relative to the gas stream during almost the whole of its motion in the funnel. The displacement of the particle relative to the gas stream during its travel down the funnel can thus be approximated from a summation of the products of increments of time and corresponding terminal velocity, as shown in Fig. 7.

It will be seen that the total displacement, or separation distance at the mouth of the funnel, is about 27 in. Under gravity alone, this distance would be not more than 20 in., which suggests that the gas-stream deceleration is about equivalent to an addition of $g/3$ in separating force.

The vertical displacement of the dust particles is greatest near the axis of the funnel, where the gas stream is free from turbulence and eddies. Particles so displaced are able to reach the relatively still gas space at the base of the dust catcher, from which they may settle out into a hopper. Around the edge ot the funnel the gas stream changes direction outwards and upwards with a further deceleration, but the strong turbulence occurring at this point probably prevents permanent separation of dust.

This form of dust catcher is thus, in principle, like an improved settling chamber in which the space is economically employed and the separating force is somewhat increased. There is little scope for improvement in performance of this type of cleaner, since the principal separating force must always be gravitational, even with the most favourable angle of funnel expansion.

Cyclone Dust Collectors

The principle of cyclonic dust separation employs part of the pressure energy of the gas to produce a vortex in the gas steam which can be arranged to impart a centrifugal acceleration to the dust particles opposed only by an inward drift of the gas stream.

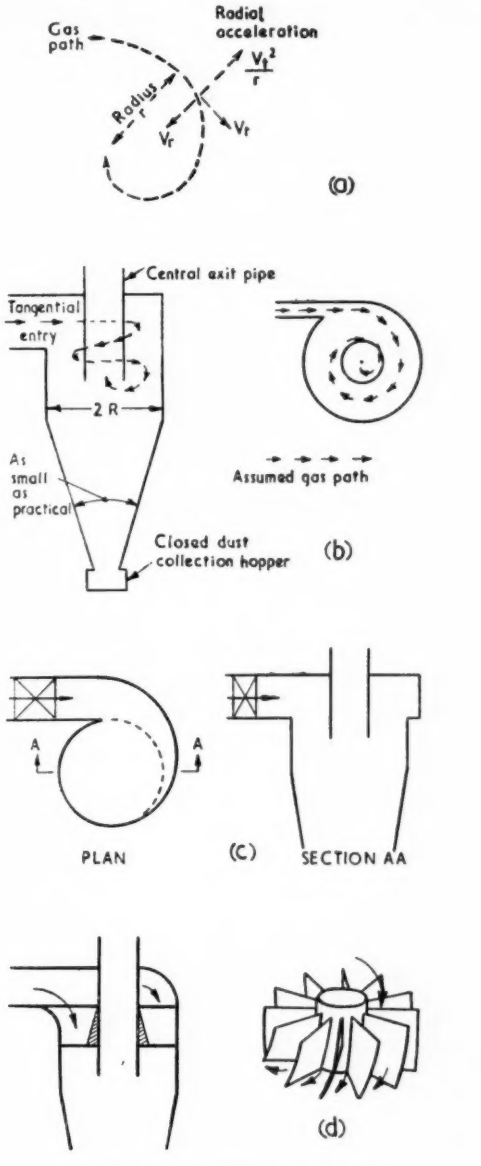
The cyclones at present used for cleaning blast-furnace gas often do not make the best use of the pressure energy in the gas. Dust catchers are commonly arranged for a combination of gravity settling and centrifugal acceleration. A typical dust catcher of this type is shown in Fig. 2*a* ; tests on this cleaner already given, showed it to be 100% efficient for particles greater than 90μ but that not more than 60–70% of the smaller particles were removed. Calculations on settling chambers and momentum dust catchers indicate, however, that only a small proportion of this dust separation may be due to centrifugal action.

True cyclones can separate efficiently far smaller particles without waste of pressure energy, but it is essential to use a correct shape of collector and to balance the size of the unit according to the volume of gas and the available pressure energy.

Principles of Operation

The gas stream in any plane of a cyclone will take the form of a spiral, and the velocity at any point can be divided into tangential and radial components, V_t and V_r, as in Fig. 8*a*.

The gas stream at any point will have, instantaneously, a radial acceleration $(V_t)^2/r$, as well as a tangential velocity V_t. This radial acceleration will impart an outward accelerated motion to all particles, resulting in an inward drag on them relative to the gas stream. It may be assumed that this drag is sufficiently large to ensure that almost all dust particles reach their terminal velocity V_T, relative to the gas stream, almost instantaneously. This terminal velocity will, of course, be determined by the magnitude of the acceleration acting on the particle, as well as by the particle dimensions. If $(V_t)^2/r$ is



(*a*) Path and velocity components of gas stream
(*b*) Diagrammatic form
(*c*) Tangential entry (ter Linden)
(*d*) Top entry with convolute vanes

Fig. 8—Design and operation of cyclone dust collectors

known, then the terminal velocity for a given particle can be determined as before by substituting $(V_t)^2/r$ as the acceleration on the particle.

The path of the particle in space will thus be defined by its resultant radial velocity $(V_r - V_T)$ and its tangential velocity V_t. If V_r is greater than V_T, the dust particle will describe an inward spiral; if V_r equals V_T, the dust particle will have a circular path; but if V_r is less than V_T, the dust particle will move towards the periphery of the cyclone and so separate from the gas stream.

The conditions to be obtained in a cyclone are thus defined. The object of cyclone design should be to ensure that for a given expenditure of pressure energy a maximum range of dust particles may be separated from the gas stream. Practical design difficulties, such as ensuring that all dust particles in the incoming gas stream have a full chance of coming under the influence of the separating force, are the main limitation of cyclone performance.

The shape of cyclone which has long been recognized as most likely to induce a nearly true vortex for dust separation is of the general form shown in Fig. 8*b*. The entry pipe should be arranged so that the whole of the gas stream and its dust content enters the cyclone as near as possible to the periphery of the vortex; this is to ensure that all the dust particles have an equal chance of remaining in the region of minimum inward gas velocity. Also, the friction resistance to the gas stream at the entry should be small.

Several different forms of entry have been proposed to achieve this compromise. The most common form is the long slit by which the gas stream enters the cyclone in a narrow stream on the periphery of the vortex; the slit extends a considerable distance down the cyclone so that its total area is not less than that of the gas main attached to the cyclone.

A second form of entry, recently proposed by ter Linden,[4] is of rectangular cross-section on the periphery of the vortex. The entry forms a decreasing spiral outside the circumference of the cyclone. It is claimed that the best results are obtained when the angle of entry is 180° (Fig. 8*c*).

In other designs the entry takes the form of an annular ring bounding the periphery of the cyclone top. A tangential velocity is imparted to the gas stream by a series of convolute vanes or deflector plates in the entry (Fig. 8*d*). The central gas outlet should be of minimum diameter, consistent with the allowable pressure drop; this permits a maximum width of undisturbed vortex annulus in the cyclone. The outlet is usually of about the same area as the entry. Turbulent eddies near the periphery of the cyclone are clearly to be avoided, since they will cause particles already at the cyclone wall to break away and rejoin the gas-stream vortex. It is therefore important to avoid any sudden changes in the cyclone shape which may induce such eddies. The particles at or near the cyclone wall will be helped to reach the collecting hopper at the base by a downward gas velocity. The value of this velocity stream is likely to diminish as the diameter of the cyclone cone decreases towards the base. Thus, the parallel body of the cyclone should be carried down as far as possible below the exit pipe, and the angle of the cyclone cone should be as small as practical.

Improved Performances from Cyclones

There is considerable room for improvement in the cleaning of blast-furnace gas by cyclone dust collectors. Firstly, the cyclone must provide a proper gas-flow pattern. It is difficult to define the meaning of this expression, but recent experimental work by ter Linden[4] (*see* Fig. 9) indicates a form of flow pattern which should be aimed at. Such a pattern, in terms of gas velocity and pressure, can be arrived at only by experiment and experience.

The desired flow pattern can be applied to all

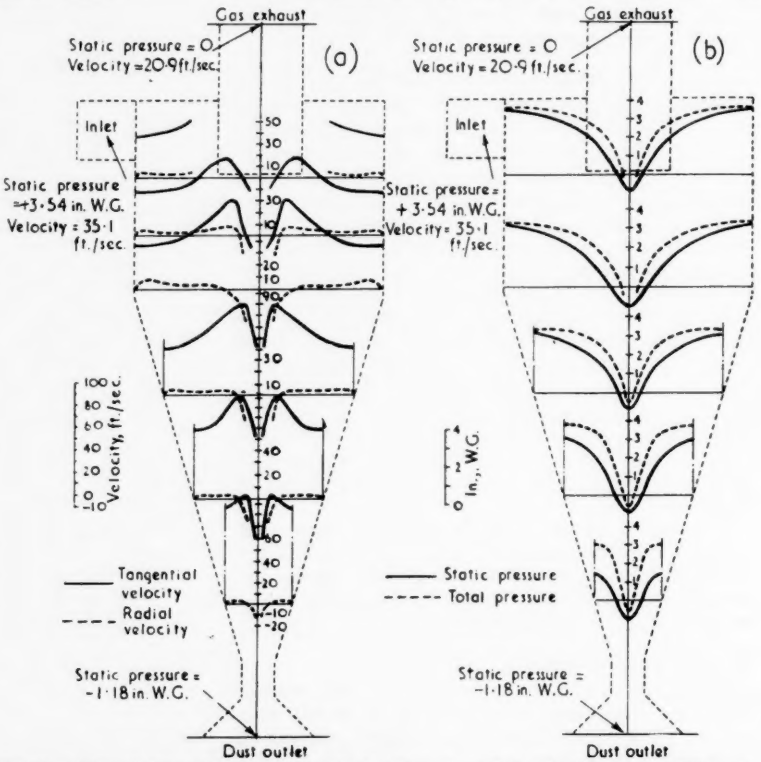


Fig. 9—(*a*) Variation of tangential velocity V_t and radial velocity V_r at different points in a cyclone (ter Linden). (*b*) Total and static pressures at different points in a cyclone (ter Linden)

sizes of cyclone by using flow volumes appropriate to the size of the unit. The flow of gas from a blast-furnace is erratic and varies widely according to the rate of blowing and the behaviour of the furnace. Cyclone dust collectors must therefore be designed to operate efficiently with considerable variation in flow rate.

The performance or separating power of a cyclone is obviously dependent upon the magnitude of the radial acceleration $(V_t)^2 r$; for a given entry velocity V_t, this acceleration will be greatest in a cyclone of minimum radius. The value of this separating power has been empirically calculated by Rosin,[5] using the following expression to estimate the minimum diameter (d) of particle which may be separated from a cyclone :

$$d = \sqrt{\frac{9\eta s(2R-s)}{\pi\sigma V 2RN}}$$

where s = distance of particle from cyclone periphery at entry
R = radius of cyclone
V = gas velocity at entry
η = gas viscosity
σ = particle density
N = number of revolutions of gas stream (assumed to be 3).

This equation assumes that the radial velocity at or near the entry is negligible and that the separating action of the cyclone is completed above the base of the gas outlet. This is not an unreasonable assumption as a first approximation, There are thus three variables which may control the minimum size of separating particle : V, s, and R. The first two are also a function of the pressure drop through the cyclone. This has been experimentally determined by Shepherd and Lapple[6] in the form

$$H_c = 7{\cdot}5\,\frac{(s.h)}{e^2} - 1$$

where H_c = pressure drop expressed in velocity heads
s = width of cyclone entry
h = height of cyclone entry
e = diameter of central outlet.

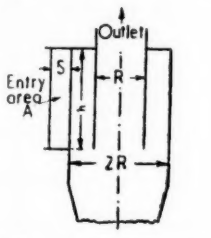
Each unit of velocity head can be expressed as :

$$H_c = 0{\cdot}003\rho\frac{(Q)^2}{A}\text{ (in. W.G.)}$$

where Q = quantity of gas flow in unit time
A = area of cyclone entry
ρ = density of gas.

The pressure drop H_c can also be expressed as $0{\cdot}003\ \rho V^2$, where V is the entry velocity.

Now the limiting factor in any cyclone system is primarily the available pressure drop. For example, suppose that the available pressure drop is 12 in. W.G. and the total gas flow to be cleaned is 2,000,000 cu.ft./hr. ; suppose also that it has been decided from experimental evidence of the flow pattern that a satisfactory form of cyclone is as shown in Fig. 10 (where all dimensions are expressed as a function of the radius R). The area of the gas entry and gas outlet of such a cyclone to pass 2,000,000 cu. ft./hr. with a pressure drop of 12 in. W.G. can be determined by Shepherd and Lapple's equation as $A = 31{\cdot}1$ sq. ft. If this figure is transposed in terms of cyclone radius, then R is 6·3 ft. and s is $31{\cdot}1/12{\cdot}6$, *i.e.*, 2·47 ft. Using Rosin's equation, the diameter of the minimum size of fully separating particle of density 2·6 g./ml. from such a cyclone will be $d = 108\mu$.



$A = \frac{\pi R^2}{4}$, $s = \frac{\pi R}{8}$, $\therefore h = 2R$

Fig. 10—Assumed cyclone dimensions

This does not imply that some smaller particles will not also be separated, but it indicates the minimum size of particle which will always be fully separated, even if initially placed at the greater permissible distance from the cyclone periphery.

If the gas flow is divided up between a number of smaller cyclones, each of similar form and each carrying an equal proportion of the gas, so that the pressure drop through the system as a whole is not increased, then the separating capacity of the system will be multiplied (*i.e.*, the minimum diameter of separating particles will decrease) in proportion to $\sqrt{R}$. This relation is shown in Fig. 11, which illustrates the theoretical advantage to be gained by the use of a number of small cyclones.

The separating capacity of a multi-cyclone system is also a function of the pressure drop. For a given form of cyclone, in which the value of sh/e^2 is constant, the pressure drop as expressed in inches of water gauge will be proportional to the square of the gas-flow rate through each unit, or proportional to V^2, where V is the gas velocity at entry.

From Rosin's equation, in a cyclone of given dimensions, the minimum diameter of separating particles will therefore be proportional to the fourth power of the pressure drop. It is thus unlikely to be economic to increase the pressure drop through the system (*e.g.*, by using fewer cyclones to carry the full gas rate)

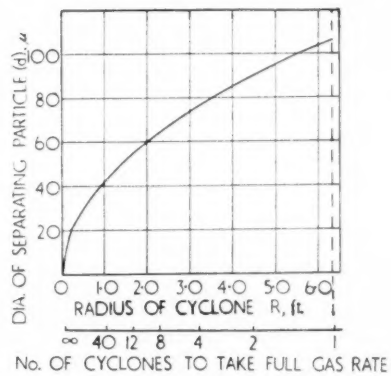


Fig. 11—Theoretical curve of minimum size of particles (density 2·6 g./ml.) separating from a multi-cyclone system passing a gas flow of 2×10^6 cu. ft./hr. (using Rosin's equation)

as a means of improving the separating power of the cyclones above the pressure energy already available in the top gas.

Future Trends

Banks of multi-cyclones have been employed for cleaning blast-furnace gas, and by properly proportioning the number of cyclones, as indicated in Fig. 11, a much improved dust separation should be possible.

In practice, such multi-cyclones may present considerable difficulties. The variable gas velocity across the incoming-gas main and the temperature variation in the gas may adversely affect the even proportioning of the gas flow between the cyclones. For example, the gas passing through the outer cyclones will become considerably cooler than that passing through the centre of the bank. Furthermore, the expansion of gas in the inlet manifold of the cyclone banks may not be uniform, so that the initial pressure energy and dust concentration is likely to be uneven. Blockage or damage to any of the cyclones will also upset the gas distribution. The complex construction of the bank is a further disadvantage in a mechanism which must be able to operate in a hot abrasive atmosphere for long periods without maintenance.

On the other hand, the advantages of small cyclones cannot be ignored. A compromise between a multi-cyclone bank, using a very large number of small units, and a single cleaner is therefore suggested. For example, eight cyclones could be arranged in two banks, each bank carrying half the gas flow and each cyclone one-eighth. Such a system should give substantial improvement in dust separation (nearly halving the theoretical minimum size of separating particle, *see* Fig. 11) without many of the difficulties associated with multi-cyclones. The gas flow would have to be evenly distributed between the banks so that each operated at its optimum capacity. A high performance under reduced gas flow could also be ensured by closing one or more of the cyclones in each bank.

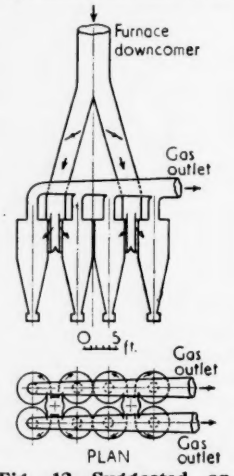


Fig. 12—Suggested arrangement for an 8-unit multi-cyclone system

A possible arrangement of eight cyclones attached to a blast-furnace is illustrated in Fig. 12.

Bag Filters

Bag filters are probably the earliest form of cleaner developed for fine particle separation. The dust-laden gas passes outward through a bank of bags composed of a suitable filter cloth. The coarse dust in the gas is caught in the meshes of the cloth and forms a finer mesh of filter which, in turn, traps the finer dust particles. The separating efficiency of the filter bags thus increases with the deposit of dust layer until even the superfine particles in the gas may be trapped. The practical limit of dust separation is determined by the permissible pressure drop through the bag and its dust layer. With a pressure drop of 9–12 in. W.G. the dust concentration may be reduced from as high as 1 grain/cu. ft. to 0·008 grain/cu. ft. The dust load on each bag is evenly distributed since the amount of gas passing is inversely proportional to the pressure drop through each bag ; thus a lightly dust-covered bag will pass proportionately more gas until the resistance of its dust layer is raised to that of the other bags.

The dust deposit is removed from the bags by shaking or by counterblowing clean gas at a pressure slightly in excess of the maximum pressure drop under normal conditions when the bag is acting as a filter. The bags are cleaned automatically, bank by bank, at short intervals, according to the build-up of pressure resistance. Each bag is designed to pass about 10,000 cu. ft./hr., but the rate of throughput may vary widely according to the dust loading of the gas and the permitted pressure drop.

The disadvantages of this system are fairly obvious. The gas must be dry, that is, above its dew-point, and yet not hot enough to char the bags, which are usually made of cotton fibre. The gas normally has to be pre-cooled in a separate tower and then reheated by a steam heat-exchanger to 80–110° C. before passing to the filter. The amount of pre-cooling will vary according to the initial temperature and moisture content of the gas. The maintenance of the desired inlet temperature may be a serious consideration in winter when the gas temperature after reheating may fall rapidly in passage through the filter because of the large surface of the cleaner exposed to the atmosphere. Such surface heat losses are usually reduced by insulation. The operation of a bag filter requires careful attention, since a fall in the incoming-gas temperature beyond certain limits can quickly stop the whole plant by permitting the dust layer on the bags to be moistened.

Changes in the dust loading of the incoming gas may necessitate changes in the bag cleaning rate, while the bags themselves must be constantly watched for wear and tear. The type of cloth must be chosen by experience, since the most suitable textures vary according to the nature of the dust. The shaking and counterblowing system is mechanically complex and must be maintained in full operation at all times.

Despite these difficulties, many bag filters are in successful use today. The experience of one company has been that the life of the filter bags was doubled and the output of the plant was increased by 25%, without change in outlet cleanliness, by using counterblowing only and so avoiding mechanical shaking. This emphasizes the importance of arranging the technique of operation to suit the conditions at each particular installation.

The construction of the cleaners is not suitable for a high ingoing-gas pressure, by reason of the shape of the casing, and thus present forms of bag filter are not suitable for furnaces having a high top pressure. This is unfortunate, since extra gas pressure might be usefully employed to overcome the resistance of the bag. At present, it is usual to boost the gas after the cleaner. This boosting (9–10 in. W.G.)

consumes considerable power (*see* Fig. 1). It is therefore thought that a modified filter system employing heat-resisting bags and a casing to withstand higher gas pressures, might be advantageously applied to plants using a high top pressure and requiring all the dust to be recovered in a dry state.

WET CLEANING SYSTEMS

Wet cleaning can be used successfully for all stages of dust removal, down to the lowest concentrations and the finest particle sizes. The mechanism of operation is that all or nearly all the dust particles in the gas contact water drops, or wetted surfaces, thereby becoming wetted, and are then removed from the system as a suspension in water.

Wet cleaning ensures that the gas is thoroughly cooled and free of any objectionable liquid present as a vapour in the top gas ; on the other hand, the separated dust has to be removed from suspension and dried before it can be re-used in any iron-making process. In existing machines the power consumption for pumping and circulating large quantities of water increases disproportionately with the degree of cleaning, and often sets an economic limit to the use of such systems. Nevertheless, there is room for considerable improvement in performance and power consumption.

Primary Wet Washers

The dust concentration of the gas entering the first stage of wet cleaning may be as high as 5 grains/cu. ft. This means that the gas entering the wet cleaner at plant *A*, for example, may contain 1,500,000 particles per cu. ft. greater than 20μ and an even greater number below this size (*see* Fig. 13). Large volumes of water (20–40 gal./cu. ft. of gas) are required to contact and remove such huge numbers of particles.

The method of using the water depends upon the design of the cleaner.

Scrubbing Towers

Scrubbing towers are simple in operation, although, as Nonhebel[7] has recently pointed out, there is little fundamental data for their design. They are usually arranged as tall vertical towers, 2–3 times the gas main in diameter and 5–10 diameters in height.

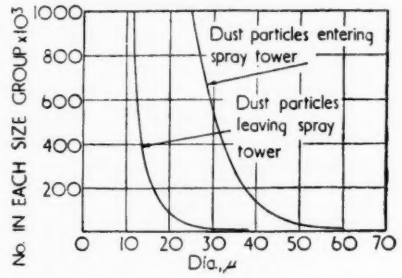


Fig. 13—Numbers of dust particles in each size group (± 5μ) per cubic foot of gas. Plant *A*. (Assumed particle density 4·5 g./ml.)

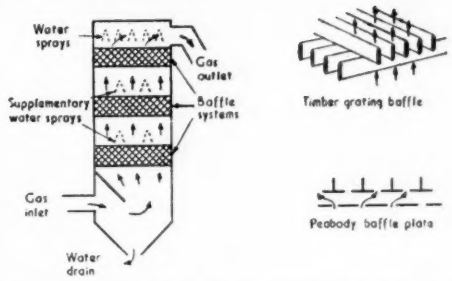


Fig. 14—Diagram of typical scrubbing tower and baffle plates

The gas enters at the base and leaves near the top, passing slowly up the tower. Water is distributed from low-pressure sprays arranged across the top of the tower.

The tower is divided horizontally into two or more sections by arrangements of baffles through which the gas has to pass and over which surface the spray water runs down. The dirty gas enters each baffle system in turn, and by many changes in direction of the gas stream and by local turbulence the dust particles are intended to impinge on the wetted surface of the baffles and are then washed out of the tower in the flow of spray water (*see* Fig. 14).

The presence of the baffles and the turbulence of the gas stream thus produces a centrifugal acceleration on the dust particles relative to the gas stream. If this acceleration is sufficiently large and the gas path is narrow, the dust particles may be expected to reach the edge of the stream and thus come into contact with the wetted surface during the short time that the gas stream is passing through the baffle system. In practice, however, the paths of the particle are not determined solely by their instantaneous centrifugal acceleration at any given point ; the changing direction of the gas stream may also induce a considerable drag on them in a new and unexpected direction. This is particularly important in estimating the paths of the smaller and lighter particles which will tend to follow closely the many changes in direction of the gas stream. This is because the small particles induce a relatively large drag, and their terminal centrifugal velocity is therefore low in comparison with the velocity of the main gas stream.

Suppose the initial gas velocity, and the particle velocity, at a point in a scrubbing tower is 10 ft./sec. in a direction x (Fig. 15). Suppose also that the gas velocity is suddenly changed to a perpendicular direction y by the presence of a local obstruction such as a baffle plate. The momentum of the particles will act in direction x, but their motion in space will be affected by the drag of the gas due to its new direction. This drag is compounded of two vector forces, one from the particle momentum in direction x, and the other from the gas momentum in direction y.

If a curved gas path is assumed in the neighbourhood of the baffle plate, then the path of a particle can be estimated by a graphical method as that due to centrifugal acceleration at successive points at different radii from an assumed centre of curvature of the gas stream. For a gas path of 0·80 in. radius

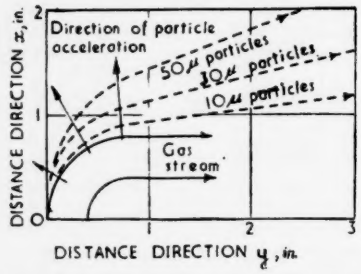


Fig. 15—Paths of particles due to curvature of gas stream (calculated). Velocity of gas stream 10 ft./sec., particle density 4·5 g./ml. All particles initially travelling with gas stream at zero distance

and velocity 10 ft./sec., the paths of various particles in space will be approximately as shown in Fig. 15.

Practical considerations, such as the prevention of blockage by sludge and resistance to gas flow, limit the minimum clearance between adjacent perpendicular baffles to not more than about 2–3 in. This space will therefore determine the curvature of the gas stream.

These examples suggest that such a baffle system cannot be expected to trap more than a small proportion of particles less than 30μ. in dia. It is, of course, impossible to determine accurately the lower limit of particle size (of a given density) which will pass unseparated through a particular baffle system. The exact form of the stream pattern is too complex for accurate analysis, and there will be some interaction between the particles themselves, but the limitations of practical cleaning can be assumed to be of the same order as the calculated examples. In practice it is found that such scrubbing towers are limited to the removal of dust down to a concentration of about 0·25 grain/cu. ft.

Spray Towers

To avoid the limitations of the scrubbing tower and to reduce resistance to gas flow, as well as to simplify design, high-pressure water sprays are injected across and up into the empty portions of the tower to impinge directly upon the dust particles. The sprays also produce a local turbulence in the gas stream, giving a more intimate mixing of the dust and water drops.

Sometimes the sprays are used in conjunction with baffles, as in the McKee and Peabody towers. One form of McKee tower incorporates a system of dish-shaped spinning plates from the periphery of each of which a curtain of water drops is thrown outwards with high initial velocity. These curtains are arranged at right-angles to the main upward flow of the gas stream in the tower. The system is similar to that shown in Fig. 16*a*. Water from a number of low-pressure sprays passes through a honeycomb of baffles at the top of the tower and then drips down on to the conical shield shown in the figure ; from the periphery of this shield the water stream is collected and falls into the circular water trough. The revolving dished plates are partially immersed in this trough, and as they spin, a layer of water is drawn upwards and outwards by centrifugal force over the upper surface of each plate. This layer of water is discharged as a curtain of drops from the periphery of each spinner.

The quantity of water spray generated in this way is not known, nor is it easy to estimate this volume or the size of the drops. The spinners rotate at about 200–250 r.p.m., so that the maximum initial velocity of the drops is about 55 ft./sec. The performance of these spinners does not seem to be better than that of ordinary scrubbing towers ; towers with spinners are used at one modern plant to clean 2,800,000 cu. ft. of gas per hour down to a concentration of 0·3 grain/cu. ft. This may be due to the low speed of the spinner rotation ; the influence of this factor is discussed on p. 319.

The Peabody tower has a similar spinner, but the spray is generated through the perforations in a rotating basket. The system, which also recirculates the spray water, is illustrated in Fig. 16*b*.

In other designs of spray tower no baffles are used and the gas stream is subjected to intensive bombardment by a large number of water jets only. For example, at plant *A*, Whessoe spray towers are employed, each having 96 $\frac{3}{8}$-in. jets spraying about 40,000 gal./hr. at a maximum pressure of 76 lb./sq. in.

Test results (*see* p. 308) show that these sprays reduce the dust concentration from 5·06 grains/cu. ft. to 0·28 grain/cu. ft. The volume of water used in the sprays is large, corresponding to 40 gal./1000 cu. ft. of gas, and the high jet pressure is probably responsible for their effective action, as the performance is reduced if the spray pressure falls.

The resistance to gas flow in the Whessoe towers is low, corresponding to only 0·5 in. W.G., and this feature, together with the simple construction, weighs considerably in their favour ; on the other hand the energy required for pumping, with frictional losses, may be 100 h.p. This is a heavy power consumption in relation to the work done, and it is probable that a large proportion of the water drops produced in the

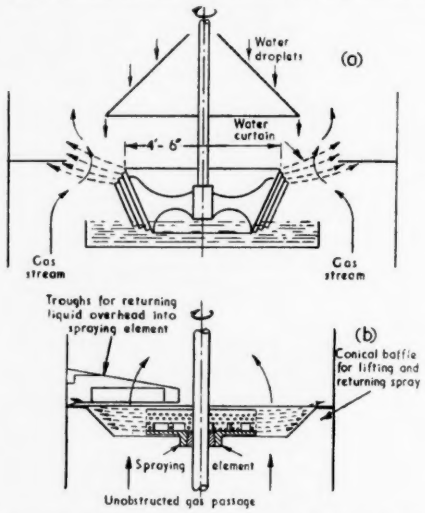


Fig. 16—(*a*) Diagrammatic section through spinner system in McKee washer. (*b*) Sectional view of Peabody rotary sprayer and elements for effecting recirculation of spray

sprays are either too small or too large to be used effectively. A further limitation is that the gas stream, which will follow the path of least resistance, is likely to avoid those areas having the highest concentrations of water drops, so that full advantage is not taken of the sprays jets.

Gas Cleaning by Water Sprays

The effectiveness of any system of water sprays for gas cleaning is primarily determined by three factors :

(1) The relative concentration of dust particles and water drops
(2) The relative velocity between the particles and drops
(3) The available time interval.

Any study of possible improvements in wet cleaning systems should therefore consider the effect of these and other factors. For instance, if the time interval is short, as is usual with a large mass flow of gas, then the relative velocity must be high for effective cleaning, unless the concentration of water drops is also very great. These principles are confirmed by a recently developed method[8] for removing dust from the air in iron-ore mines after blasting. In this method a very fine mist is introduced into an atmosphere heavily laden with dust, and the mist drops circulate slowly with movements induced by the cooling of the warm air. The rate of dust removal is slow, but after about 15 min. the burden in the air is almost wholly removed. The concentration of the finest particles, from 5μ down to 0·2μ in dia., is reported to fall from 227,000 particles per c.c. down to 30,000 particles per c.c. Such particles could not be separated by gravity in any reasonable time.

Local air movements induced by cooling and condensation play a large part in this method, and it is reported that the rate of separation is substantially improved by the addition of a very small quantity of castor oil to the spray mist ; this ensures good adherence between the water drops and dust particles.

In spray towers the time interval for dust separation is short, perhaps 10–15 sec., according to the scale of the plant, and the relative velocity between spray drops and dust particles must be correspondingly high to ensure removal of the finer dust particles. The water drops can maintain their high velocity only over a limited distance as their momentum is opposed by the large viscous drag of the gas stream. Thus the drops must be given an extra large initial velocity if a desired average velocity is to be maintained over a given distance. Values of the initial velocity required can be determined for particular drops travelling a given time or distance in still blast-furnace gas, from the curves given by Stringer. For example, if it is required that the drops in a spray tower shall retain a relative velocity of 4 ft./sec. at a distance of 5 ft. from the spray jet, the corresponding initial velocity can be estimated as follows :

Water-drop dia., μ	300	200	150	100
Initial jet velocity, ft./sec.	170	470	1070	2800*
Terminal velocity under gravity, ft./sec.	1·9	1·4	1·0	0·5

* This figure, of course, cannot be attained as it is above sonic velocity.

The jet velocity of a 200-lb./sq. in. spray is not likely to exceed 170 ft./sec., as calculated from pressure

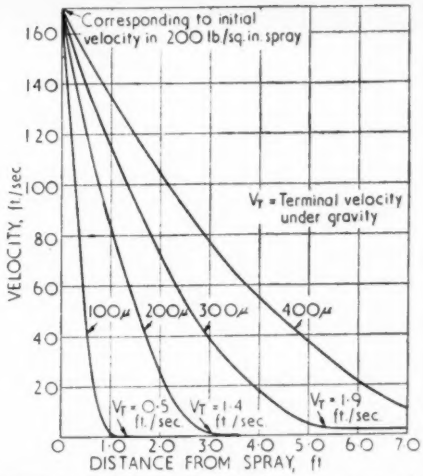


Fig. 17—Velocity/distance curves for various sizes of water drops

energy, and therefore only water drops greater than 300μ in dia. will maintain the desired velocity at a distance of 5 ft. from a high-pressure spray ; in practice, however, such a spray will include a high proportion of smaller drops. Assuming that an initial velocity of 170 ft./sec. is imparted to all drops, the degradation of drop velocity with distance can be calculated for all sizes. This is illustrated in Fig. 17 for drops of 400, 300, 200, and 100μ in dia. These curves show that drops less than 200μ in dia. are unlikely to be of use in the operation of high-pressure spray towers because they cannot retain for a reasonable distance the high velocity necessary for the rapid impingement of a large number of dust particles. It may also be noticed that the terminal velocity under gravity of all these drops is less than the average gas velocity observed in most towers so that they are likely to be carried up out of the tower with the gas stream. The importance of the smaller drops in a concentrated spray is further limited because they become agglomerated very rapidly. After about 2 ft. of travel, very few drops less than 300μ in dia. will exist as separate bodies ; they will almost all become attached to the more rapidly moving larger drops.

While most of the drops generated in a high-pressure jet are less then 200μ, nevertheless the main weight of the spray comes from relatively few large drops. The following figures are quoted by Perry[9] as typical of the drop distribution from a high-pressure jet :

Proportion of Drops Produced by a Hollow Cone Nozzle of 0·125 in. dia. at 200 lb./sq. in.

Nominal Drop Dia., μ	Number of Drops	Weight Distribution, %
10	100	0·01
25	50	0·1
50	45	0·7
100	27	3·5
150	15	6·5
200	11	11·3
300	6	21·0
400	3	24·8
500	2	32·1
>200	8·5%	89·2

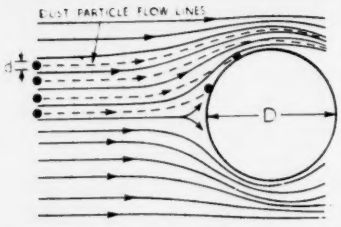


Fig. 18—Gas-flow lines around circular obstruction

Thus 89·2% of the mass of the spray will contain only 8·5% of the total number of drops. Now, the mechanism of gas cleaning by spray impingement requires a maximum number and concentration of drops so that if, as in the present design of spray towers, only the largest drops can be effectively used, then the total volume of water required will be very great. A further limitation on the use of relatively large water drops is that they are not easily brought into contact with the fine dust particles. The disturbance in the gas caused by the motion of each drop will deflect away the dust particles in its close vicinity. Possible paths of particles close to water drops are illustrated in Fig. 18, from which it is seen that not all the dust particles in the volume swept by a water drop will be impinged; this effect is increased with the disparity in size between the two bodies.

The actual paths of spherical particles travelling close to a cylinder and an aerofoil have been calculated by Glauert,[10] and from these calculations she has deduced the percentages of particles of diameter d in the area swept by the cylinder which will impinge upon it. Comparative proportions for spherical dust particles in the neighbourhood of spherical water drops are obtained by squaring Glauert's figures to allow for a third dimension.

A curve relating percentage impingement to the relation between drop and particle diameters and relative velocity is given in Fig. 19; the relating function is $(d^2/D)V$. For maximum contact between drops and dust particles it is therefore clear that their disparity in size should be small and their relative velocity should be large.

The performance of the present form of spray tower is thus handicapped by a number of inherent factors, and there are few fundamental reasons for retaining their conventional design, which has probably evolved from their use as gas coolers.

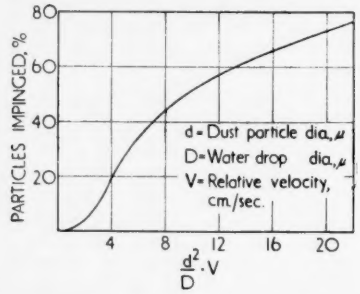


Fig. 19—Relation between percentage impingement, size and velocity of particles (calculated)

New Designs of Wet Cleaner

The foregoing analysis suggests a number of lines for improved designs to give better performance with lower power and water consumption.

If normal forms of high-pressure spray jets are used, then the maximum distance between the spray source and any part of the gas stream should not be greater than about 1 ft.; this would ensure that the large numbers of small water drops less than 200μ in dia. can be used effectively (*see* Fig. 17). The disposition of the jets should also be arranged so that the concentration of sprays may subject all parts of the gas stream to an equally intense bombardment by water drops. The total amount of water per unit of gas may thereby be reduced without loss of cleaning efficiency. The advantage to be gained by reducing the volume of the wet cleaner, and thereby the maximum distance between sprays, would appear greatly to outweigh the resultant increase in gas velocity and resistance to flow.

Recent examples of the application of these conditions are reported in the following two sections.

Venturi Gas Cleaner

The venturi gas cleaner is in operation in the U.S.A. and has been described by Kopecki.[11] In this plant the gas from two experimental open-hearth furnaces carrying an average of 2·6 grains/cu. ft. of very fine fume (reported to have an average diameter less than 0·2μ) is claimed to be cleaned to 0·13 grain/cu. ft. (more than 95% of its dust content removed) in a wet cleaner of the form diagrammatically illustrated in Fig. 20.

The gas stream enters the venturi pipe at a rate of 2,400,000 cu. ft./hr. measured at N.T.P. (approximately equal to the gas volume from one large blast-furnace). A large number of sprays are arranged near the point of minimum diameter, 60 on the circumference of the pipe in two rings of 30, and seven larger nozzles arranged equidistantly across the cross-section of the pipe. The maximum distance between spray sources in any direction is thus not greater than about 6 in.

The quantity of water used is about 3·75 gal./1000 cu. ft. of gas, and it is injected at relatively low pressure. A high relative velocity between the dust particles carried in the gas stream and the water drops in the sprays is achieved by the high speed at which the gas stream passes through the throat of the venturi. It is claimed that "The gas impinges

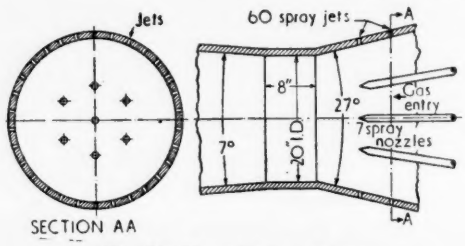


Fig. 20—Venturi wet cleaner

upon and disrupts the rather large jets of liquid and accelerates the drops of liquid with probable further disruption so that much of the liquid is finely atomized, at least briefly." This description does not wholly explain the action of the cleaner, as it would also seem that the inertia drag of the water particles against the relatively fast-moving gas stream is likely to account in a large degree for the very favourable gas cleaning which is claimed. The dimensions of the venturi are such that, assuming an average gas temperature of 200° F., the average gas velocity in the throat of the venturi is probably as high as 450 ft./sec. The main energy consumed in the cleaner is therefore due to that part of the work done in accelerating the gas which is not fully recovered in the expansion of the venturi. The pressure drop across the venturi is 9 in. W.G. at full gas rate, which would appear from calculation of kinetic energy to be almost all due to the loss of unrecovered energy in the venturi.

The boosting compression of the gas by this amount would require at least 90 h.p., although it is reported that 400-h.p. fans are used. This energy consumption detracts somewhat from the advantages of this form of cleaner.

Stationary Disintegrator

A wet cleaner, known as the stationary disintegrator,[12] has been developed, which incorporates a contraction similar to that of the venturi cleaner, to concentrate the dust particles and to bring them all into the close vicinity of high-velocity water drops. The gas is drawn through a relatively small injector pipe of venturi form. Each unit is designed to deal with about 60,000 cu. ft. of gas per hour, and an injector action is achieved by a high-pressure water jet which discharges about 12 gal. of water per minute at 150 lb./sq. in. through and along the centre of the venturi pipe. This water jet, with a probable initial velocity of 130 ft./sec., will induce a certain increase in the speed of the gas passing though the venturi. The water drops from the expanding spray jet in the pipe are broken up by contact with a curved baffle, and the water streaming off this plate is thus deflected across the whole of the gas stream as it emerges from the venturi (Fig. 21).

It is stated that, when the cleaner is used in conjunction with a primary cleaner, the dust content of blast-furnace gas can be reduced from 2 grains/cu. ft. to 0·02 grain/cu. ft. The pressure drop across the mechanism is probably accentuated by the very large volume of water which 'fills' the core of the injector pipe, partially blocking the gas passage.

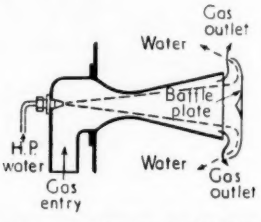


Fig. 21—Stationary disintegrator

It is also probable that much of the water in this jet is ineffective since the relative velocity between the gas and the water jet in the pipe is small (both are moving in the same direction), and as the water streams off the edge of the baffle plate it is unlikely to be properly divided into drops of an effectively small size. This point is emphasized by the fact that the baffle plate has a toothed edge, presumably to improve the distribution and division of the water stream.

The pressure drop across the mechanism is stated to be 12–16 in. W.G., while the water consumption is 12 gal./1000 cu. ft. These considerations have to be set against the reported effective cleaning by this system.

Future Trends

All the foregoing designs of wet cleaner seem to indicate the difficulties of arranging an even, high concentration of water drops in contact with dust particles without severely constricting the gas stream and so raising the pressure drop through the plant beyond an economic limit. A form of spray generator which will avoid these objections therefore appears to be desirable. It is well known that a spinning plate can be used as a spray generator and this method has been successfully employed in several designs of wet cleaner (*see* p. 316). Its success, however, has been somewhat limited; this may be due to the lack of satisfactory design data, particularly in respect of water-drop formation.

In recent years the formation of drops generated from the edge of horizontal spinning discs has been experimentally determined by Walton and Prewett,[13] who found that, for small discs rotating at very high speeds, and for small quantities of water, a substantially constant size of drop may be thrown off the periphery of the spinner.

The diameter of the drops is given by the equation :

$$d = K.\frac{1}{W}\sqrt{\frac{T}{D\rho}}$$

where d = drop diameter (cm.)
W = angular velocity of disc (radians/sec.)
T = surface tension of liquid (dynes/cm.)
D = disc diameter (cm.)
ρ = density of liquid (g./ml.)
K = constant (for water, $K = 3{\cdot}8$)

It is not known how far these data can be applied to the performance of larger spinners and for larger volumes of water. Futher experimental results would be necessary to assess their practical value, but direct use of the above equation suggests that to produce a spray composed of 200-μ drops from a 12-in. dia. disc will require a rotational speed of 2800 r.p.m. This is very much greater than the speed of conventional spinners, but the initial velocity of the water drops will be correspondingly higher, and the drops will all be of a uniform small size. The power required to drive the spinner and to accelerate the water spray is likely to be less than that needed to produce the pressure behind a conventional water jet, but here again experimental evidence is lacking; when this is available there would appear to be considerable scope for the design of an improved wet cleaner using this principle of spray generation.

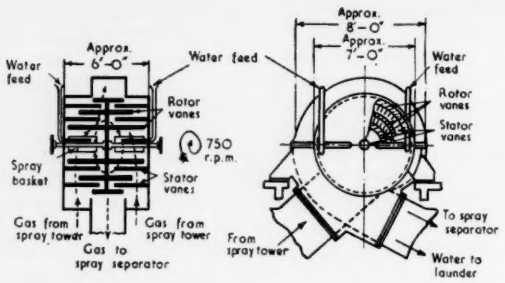


Fig. 22—Diagrammatic arrangement of disintegrator at plant *A*

Centrifugal Disintegrators

The present-day form of disintegrator can be said to have evolved almost directly from that of an irrigated centrifugal fan.

Disintegrators are essentially constructed as a number of concentric beater-bar rings enclosed in a case. These rings are arranged as alternate stators and rotors so that the whole system can present a number of rapid changes in relative motion in a radial direction. The gas stream enters at both sides of the machine, inside the periphery of the smallest circle of beater bars, and is drawn outwards through the beater-bar system by centrifugal fan action of the rotors which revolve at high speed.

Water is introduced into the centre of the machine through a rotating spray basket by which it is spread over the inner circle of rotor bars. The interaction of the rotor and stator bars divides this water into drops, which are generated as the water is spun off each successive ring of the rotor. The rotor revolves at about 750 r.p.m.

The arrangement of the disintegrators at plant *A* is shown diagrammatically in Fig. 22.

Performance and Power Consumption

The test results at plant *A* show that disintegrators can clean gas to 0·006 grain/cu. ft., although the performance is probably affected by the dust concentration in the inlet gas.

The power consumption of present designs of disintegrator is very high. For example, disintegrators at plant *A* require 600 h.p. to handle 2,000,000 cu. ft. of gas per hour, measured at N.T.P. As much as 80% of this power is used in the turbulent beating of the gas, since, when no water is supplied to the machines, the power consumption is still about 490 h.p. Curves of power consumption in a similar disintegrator at plant *C* are given in Fig. 23.

The water sprayed into disintegrators is given a high velocity by the rotor bars; this velocity will reach a maximum value on the periphery of the outer circle of the rotor, where the water velocity may be assumed equal to that of the rotor bars. In a 6 ft. 0 in. dia. machine at 750 r.p.m. this will reach 280 ft./sec.

The useful work on the water stream may thus be considered to be that required to impart this peripheral speed to the whole mass of water. A machine treating 2,000,000 cu. ft. of gas per hour is usually supplied with about 13,000 gal./hr., and the kinetic energy to accelerate this quantity to 280 ft./sec. will require 56 h.p. The efficiency of work done on the water may thus be expressed as that to accelerate the water divided by the extra power consumption of the machine when supplied with water. For the test conditions at plant *C*, this efficiency is shown in Fig. 23. The work done on the gas, expressed as a rise in pressure, during its passage through the disintegrator at plant *C* is also shown in Fig. 23. At minimum gas flow the rotation of the vanes boosts the gas pressure about 9·0 in. W.G., but this falls to not more than 6·5 in. at a gas rate of 3,000,000 cu. ft./hr. The efficiency of this pressure boost is low, since the power consumption of the machine (without water) at 3,000,000 cu. ft./hr. is 320 h.p. This may be compared with the 70 h.p. required in a typical boosting fan (*see* Fig. 1) at the same gas rate. Most of this pressure increase is in any case used up when the cleaned gas passes through the subsequent spray separator.

The main waste of power in the disintegrator clearly arises in the friction between the rotor and stator blades and the gas stream. This friction occurs in the large number of small gas passages and small clearances between the stator and rotor vanes of the machines. The possibility of reducing this power loss without impairing the effectiveness of the machine as a cleaner is well worth considering, since it has been stated that 88% of the operating cost of these machines is due to consumption of electrical power.[2]

Principles of Operation

There are several possible explanations of the mechanism of gas cleaning in these machines. Primarily, the water drops bombard the dust particles in the gas stream, and by reason of its high velocity each drop is able to come into collison with a large number of particles; very large numbers of small drops are produced by the spinning of the rotors and these are flung outwards across the gas stream as it is concentrated near the periphery of the casing.

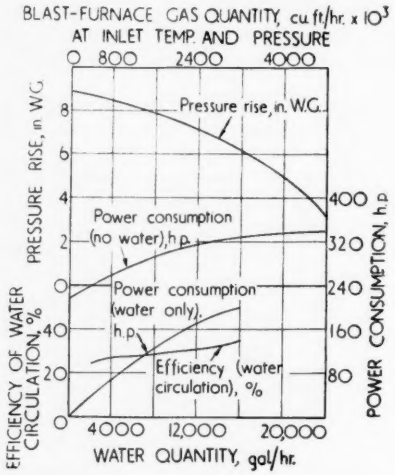


Fig. 23—Characteristic curves of disintegrator at plant *C*

The wetted dust particles are then separated from the gas stream by impingement on the casing wall.

The high peripheral speed of the outer rotor ring would suggest that the water drops produced are much smaller than those formed in a spray tower. This means that the number of drops per gallon of water is correspondingly greater. The smallness of these drops, however, should not detract from their effectiveness since their path of travel is likely to be short. Thus, they can maintain a high velocity and a high rate of dust impingement across the small gas space between the rotor and the disintegrator casing.

Secondly, a high degree of local turbulence is generated in the gas stream, which causes the dust particles to be thrown to the edges of the numerous vortex eddies so that a proportion of the dust will impinge directly upon the stator vanes and casing. Thirdly, the wetted surfaces of the rotor vanes sweeping through the gas stream will strike the suspended dust particles. This mechanism may be unimportant, as the great disparity between the dimensions of the rotor vanes and the particles reduces considerably the chances of such direct collision (*see* Glauert effect).

The total number and the concentration of water drops in relation to the path of the dust particles in the gas stream have a marked influence on the performance ; the exact form of the gas and water paths is not known, but it has been found in one particular machine that when the peripheral area of the outer circle of rotors was increased, with the object of giving greater gas capacity to the machine by doubling the length of the rotor vanes, the concentration of dust remaining in the gas after passing through the machine was increased threefold. This suggests that a high concentration of water drops on the periphery of the machine is essential if substantially complete dust separation is required.

The importance of the inner rings of rotor vanes is not so clear ; it may be that their principal function is to ensure that the water supply is evenly distributed over the whole periphery of the machine.

Future Trends

There appears to be almost no fundamental data for the design of disintegrators, this type of machine having developed by experience from the original forms of irrigated fan, and it is only in recent years that the heavy power consumption of these machines has been seriously criticized.

The dearth of design data or knowledge of the exact function of the various parts of the machine makes it difficult to suggest how the power consumption can be appreciably reduced without affecting the excellent performance obtained from existing machines. It is possible that an alternative system of water distribution could be devised to be as effective as the present arrangement, with a lower resistance to gas flow. This might mean that the inner rotor ring could be removed without reducing performance. These possibilties can be assessed only by practical trial, and it is considered that investigations should be undertaken to determine the fundamental characteristics, such as drop sizes, concentration, and velocity of the sprays produced from various forms of large-scale spray generators, including the form of rotor vanes used in present-day disintegrators.

ELECTROSTATIC PRECIPITATORS

Electrostatic precipitators for fine gas cleaning have been widely adopted in the steel industry for a number of years, but only in the last decade has the design of these plants developed to the point where the fundamental advantages can be fully realized in practice.

Performance

Present designs of precipitator, both plate and tube types, are highly efficient, and it is possible for such plant to clean blast-furnace gas to a concentration as low as 0·002 grain cu. ft.

The performance of the precipitator is closely dependent on the rate of gas flow through the plant and also to a smaller extent on the incoming dust concentration. These two factors have a particularly large influence, since the mechanism of operation (discussed in the following section) is based directly on the separating velocity imparted to the dust particle in comparison with the speed of the gas stream passing through the plant.

The presence of moisture also has an important effect on the performance of precipitators. If the moisture content is low the dust may also be non-conducting so that back-ionization occurs, which may lead to flash-over. Sharp-pointed dust deposits may also give rise to back-ionization accompanied by flash-over. This condition may also arise where there is ample moisture in the gas, the particles deposited on the collecting electrodes forming into a mass of fern-like structure which cannot easily be removed ; the presence of such a deposit on the electrodes will substantially reduce the passage of electrical energy and may even cause a breakdown in the electric potential if the gas gap between the electrodes is appreciably reduced, leading to the complete failure of the plant. This has been a source of considerable trouble in some past designs.

It is therefore common practice to arrange that precipitators for cleaning blast-furnace gas, in which a certain amount of moisture is inevitably present, are operated in a fully wet condition. Sufficient water is added to the incoming gas and sprayed over the collecting electrodes to ensure that all the dust may be continuously washed away as it is deposited.

If these conditions are obtained it is possible to match the practical operation of the plant quite closely to the theoretical principles of separation. The amount of water added to the gas may be as much as 7 gal./1000 cu. ft., but no high pressure is required. The electric power consumption is sufficiently small to be an unimportant item in the total operating costs.

Principles of Operation

The theoretical aspects of electrostatic dust separation have been extensively examined by many workers since the original work of Lodge and Cottrell. In a recent review D.V. Onslow[14] has described the basic

functions of an electrostatic precipitator in the following terms :

" An electrical precipitator consists of a discharge electrode of a relatively small diameter, such as a wire, and a collecting electrode, such as a plate or tube, between which the gases carrying the dust particles are driven. The discharge electrode is made the negative pole, whilst the collecting electrode is maintained at earth potential, and a unidirectional high potential electric field is set up between them. At a critical voltage a corona discharge takes place at the surface of the negative electrode, resulting in the emission of electrons which, under the influence of the electric field, strive to reach the electrode of opposite polarity. The negative ions coming into collision with the dust particles in suspension, attach themselves to them and cause them to migrate towards the earthed collecting electrode. On reaching the surface of this electrode the particles give up their charge and adhere lightly to it until dislodged by rapping or otherwise agitating the electrode. A certain number of dust particles which come within the zone of ionization become positively charged and are drawn to the discharge electrode, which also becomes coated with dust and has to be rapped periodically, otherwise the deposit will reduce the emission of ions to a point where insufficient are available in the field to convey the current and charge the suspended particles. The precipitated material, when dislodged, falls into hoppers underneath. From this it will be seen that four steps are necessary :

(1) Charging the particles by means of gaseous ions or electrons
(2) Transporting or precipitating the charged particles through the gas to the collecting electrode by the force exerted on the charged particles by the electric field
(3) Discharging the charged particles
(4) Removing the precipitated material from the electrode

" Both the maximum charge acquired by the particles and the rate of charging depend on the size of the particle, the ionic concentration, and the strength of the electric field ; and as the velocity of a charged particle in the electric field depends on its charge, it is desirable to secure the maximum number of ions on every particle. All particles of a given size, however, do not obtain the same charge because the dielectric constant varies, the field is not uniform over the ionizing region, the shape of the particles varies, and the equations governing the charging of small particles depend on the probable movement of ions, and in the smaller particles the numbers involved are too small to give a uniform result. A complication is introduced in the conflicting requirements that the electrostatic field should be non-uniform in the interest of an optimum ionization, that is, of the charging process, while this same field should be of uniform intensity for the sake of greatest efficacy of particle propulsion towards the collecting electrode.

" There are two forces acting on the particles, one at right-angles to the other. The first is set up by the flow of the gas which carries the particles along between the two electrodes, and the second force is that produced by the ionization of the gas and dust particles which is on a plane at right-angles to the electrodes, and causes the ionized particles to drift towards the collecting electrode. It is the resultant of these two forces which moves the particle across the space between the electrodes." [The course followed is of the character shown in Fig. 24.]

" Assuming that the particle first comes under the influence of the electric field at A, near the discharge electrode, it will move along the line AB towards the wall of the collecting electrode. If, however, the gas velocity is too high, or the voltage (and consequently the ionization effect) is too low, the particle

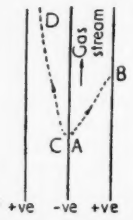


Fig. 24—Possible paths of dust particle in electrostatic discharge tube under interaction of electric field and gas stream velocity

will move along a course as indicated by the dotted line CD, which means that it will pass out of the end of the tube before it reaches the wall, and escape.

" It can be seen, therefore, that the efficiency of the removal of any given dust particles depends primarily on the gas velocity, the diameter and length of the electrodes, and the applied voltage.

" The three most important considerations in electrical precipitation are cleanliness of the collecting electrode, equal distribution of the gas over the electrodes, and the corona discharge. If a deposit is allowed to build up on the collecting electrode, back-ionization is produced which is extremely detrimental to the efficiency and may even stop the whole precipitation action. It tends to impart a reverse polarity to the particles and so to project previously deposited particles back into the gas."

The discharge electrode is always arranged to be negative since the negatively charged ions, being lighter than the positive ions, reach in a shorter mean path the speed required for ionization of the gas molecules by collision. This means that the maximum current for negative corona will be greater than for positive discharge and the breakdown potential will also be higher.

The intensity of gas ionization in the corona discharge is so great that a limiting charge is very soon reached on all dust and moisture particles. For practical purposes it may be assumed that the dust particles receive their limiting charge almost instantaneously ; for example, it has been shown experimentally that 90% of the limiting charge can be received in 0·1 sec.

Not more than about 1% of the total current in the precipitator is carried by fully charged dust or moisture particles, even at the highest concentrations, but the presence of these bodies reduces considerably the total current flowing across the precipitator since the relatively large and slowly moving dust

particles produce a large negative space charge. The precipitated layer of dust particles adhering to the collecting electrode may cause a further important change in the total current.

The mathematics of corona discharge are well known from electric theory. The density of ions n in a cylindrical condenser, corresponding to the tube in a precipitator, is given as :

$$n = \frac{ER\pi e}{4}$$

where E = field strength
R = radial distance from electrode
e = basic charge on ion

The field strength E is calculated as :

$$E = \sqrt{\frac{2i}{b}}$$

where i = current per centimetre of length of tube
b = mobility of ions

In practice, i is about 0·01 m.amp./cm. and $b = 600$ e.s.u. The limiting charge accepted by an ion has been given by Ladenberg as :

$$ne = E_0 K r^2,$$

$$\text{where } K = 1 + 2\left(\frac{\epsilon - 1}{\epsilon + 2}\right)$$

E_0 = field intensity at particle
ϵ = dielectric constant
r = radius of particle

(For superfine particles, less than 1μ in dia., the limiting charge is

$$ne = 2 \times 10^6 . r$$

and the total charge on such particles is independent of their dielectric constant and the field strength.)

The total force on a fully charged particle will be

$$E.n.e. \text{ or } E.E_0 K.r^2$$

Using the notation employed by Stringer,[3] this can be equated as :

$$EE_0K\frac{d^2}{4} = \frac{4}{3}\pi\frac{d^3}{8}\sigma f$$

where f is the acceleration associated with the force on the particle.

$$\therefore \quad f = \frac{3}{2}\frac{E.E_0K}{\pi d\sigma}.$$

Thus, if the field strength at the collecting electrode E, and in the charging zone E_0, and the dielectric constant of the particle, are known, then the motion of the particle can be determined in still gas by reference to the graphs given by Stringer. This calculation, of course, assumes that the particle acquires its limiting charge instantaneously ; also, the motion is theoretically independent of particle density.

The motion can also be calculated, as a first approximation, for particles less than 25μ dia. in terms of terminal velocity. For example, if E and E_0 are assumed equal to 10 e.s.u. (or 3000 V./cm.), *i.e.*, the particle is initially close to the negative electrode and $K = 2{\cdot}0$, then the velocity of a 10-μ particle can be determined as about 1 ft./sec.

In practice, however, it is difficult to estimate accurately the field strengths and the dielectric constants of the many materials found in the dust. The motion of the gas in the precipitator tube will be turbulent and this will disturb the ideal system of forces assumed in Fig. 24. Also the 'electric wind' caused by the motion of the ionized gas molecules towards the collecting electrode may impart a velocity to the dust particles relative to the gas stream apparently higher than that calculated from theory. The effectiveness of the precipitator, in respect of particular particles, may also vary according to their initial position in relation to the collecting electrode.

The maximum horizontal distance which the particle may have to travel is from the central discharge wire to the collecting electrode. Very few of the particles, however, will travel this full path as they may be assumed to be initially disposed across the gas stream in proportion to the square of the distance from the centre to the wall of the precipitator tube. Thus, the precipitator is likely to be more efficient for a concentration of particles than if the worst case of a single particle is considered.

The performance of individual precipitator tubes operating with a given electric force is therefore determined from experience in terms of the dust concentration remaining at the gas outlet, or according to empirical expressions, such as that of Deutch, where efficiency of precipitation on a weight basis is given as :

$$\gamma = 1 - e^{-\frac{k}{V}}$$

where k = constant
V = gas velocity

The capacity of a particular design is thus calibrated in terms of the outlet concentration in relation to the gas velocity. On such a basis the characteristics of the forces on the dust particles, such as field strength, dielectric, electric wind effect, etc., remain undetermined, but may be assumed to have a consistent influence on the separation of the dust in a particular installation.

Future Trends

Theoretical and practical knowledge of the principles and operation of electrostatic precipitation appears to be well advanced, and this suggests that the present basis for electrostatic cleaning is unlikely to be modified.

Further improvements over the present limits of operation therefore appear to lie in many practical details of design, such as the form of water spray for irrigation and flushing, the form of electrode wire to give improved corona discharge over long periods, and the arrangement of the whole plant to economize in ground space with maximum performance (*e.g.*, longest duration of gas stream in the precipitator) over a wide range of gas-flow rates.

CONCLUSIONS

(1) There is very considerable scope for the improvement of gas-cleaning plant in terms of mechanical power consumption, water consumption, and space requirements. Such economies, however, must not be allowed to reduce the present high overall performance of plant.

(2) The performance of plant cannot be satisfactorily measured for the purpose of examining the

mechanism of cleaning on the basis of dust weight concentration alone. The concentration, size, and density of the dust particles must also be taken into account. A simple method of distinguishing the effective size and density of large numbers of dust particles in a gas is therefore required for use in conjunction with the present system of dust sampling by weight.

(3) The obstacles to the improvement of plant performance suggested in conclusion (1) appear to be the lack of adequate performance data and the paucity of basic design data. This applies particularly to wet-cleaning systems where the greatest improvements in design appear to be possible. It is suggested that investigations into the behaviour of water sprays and mists, applied to gas cleaning, would be essential to such improvement.

(4) There does not seem to be any single arrangement of coarse and fine cleaners which is clearly superior, both in cleaning performance and in energy consumption. A variety of arrangements is permissible to produce economically an overall clean gas. The pressure energy of the top gas, however, does not appear to be advantageously used in many designs of cleaner, and it is suggested that the advent of high top-pressure operation should permit a larger proportion of the cleaning load to be carried in those cleaners which operate by the use of pressure energy.

APPENDIX

GAS SAMPLING AND DUST ANALYSES

Ensuring a True Sample

To determine the concentration of dust in blast-furnace gas it is essential to obtain a representative sample of the gas and its dust content. This is not simple, particularly if the dust carried in the gas stream is composed of particles of many different sizes and of variable density. To obtain a true sample of the dust in the gas stream, in respect of both particle size and concentration, the gas at the sampling point must contain a representative sample of the dust and the mechanism of sampling must not disturb this concentration.

Much thought has been given to these problems, and their solution is still under review. The latest opinion on this subject is expressed in the British Standard Code on the sampling and analysis of the gases which, at the time of writing, is being circulated for comment in draft.[15]

The sampling of gas-borne particles in a blast-furnace gas main presents special difficulties. Because of the size and nature of the plant and the relatively low dust concentrations, it is convenient if the sample can be taken over a reasonably long period at a single fixed point in the system. Experience has indicated[16] that by using an appropriate baffle in the gas main, such as an orifice plate of diameter equal to the main-diameter/$\sqrt{2}$, the dust content of the whole gas stream can be sufficiently concentrated at the centre of the main at a distance of five pipe diameters downstream from the baffle to permit a representative sample to be taken at this single point. This method has been found most successful, but its disadvantage lies in the pressure resistance of the baffle which may be as much as five velocity heads. Such a pressure drop is not permanently permissible in most gas-cleaning systems, but it may be possible to design this baffle in the form of a butterfly valve so that it can be swung out of the way when not in use.

These difficulties have meant that the routine samples of dust from gas-cleaning plant are usually taken from the material separated and collected at various points in the cleaning systems, *e.g.*, dry deposit in the dust catchers; sedimented deposit in the water drained from spray towers, disintegrators, and precipitators; and dry deposit from filter bags.

Such samples are, of course, average bulk samples, each representing the particular fraction of the dust separated by a part of the cleaning system. The total of all such samples in a whole cleaning system represents weight of dust removed from the gas, but it does not necessarily truly reflect the numerical concentration of particles at a particular point in the gas stream.

The dust content of the final cleaned gas must be sampled from the gas stream; fortunately the dust remaining in the gas after cleaning is sufficiently small to be extremely stable in the gas stream and is not readily disturbed by local gas turbulence, so that a true sample is readily obtained. The very low concentration of particles in the cleaned gas also requires that the sample shall be drawn from a large volume of gas (*e.g.*, over a long period through a small sample probe) so that the resulting average sample may be used to complete the average dust content of the dirty gas obtained from a summation of the bulk samples of dust separated from the various parts of the cleaning system.

The sampling of dust particles in liquid suspension is a relatively easy task, provided that the sample is taken from a point immediately close to the free flow of water from the cleaner and not after the dust has been able to settle appreciably from suspension. The settling rate of dust particles from water is sufficiently slow for it to be assumed that no sedimentation will occur in a fast-moving stream.

Dust Concentration

Dust concentration from a gas-borne sample is usually measured by direct weighing of the filter paper or other filtering medium upon which the dust is deposited as it is sampled out of the gas main. The volume of gas drawn through the filter is also directly measured by passage through a gas meter.

There is little difficulty in obtaining an accurate measure of dust concentration since, if the concentration is low, the gas sample can be made proportionately larger. The presence of moisture, however, must be carefully avoided by arranging that the whole equipment is maintained well above the dew-point of the gas during sampling.

Particle Size Analysis

It has been suggested in this paper that measurement of dust concentration alone is insufficient to give a satisfactory analysis of gas cleaning and that the effective sizes of the dust particles should also be taken into account. The coarser particles, above 75μ, can be directly measured by screeening on fine-

mesh sieves, but below this size other methods must be used. The fine, sub-sieve, particles constitute almost the whole problem in gas cleaning ; there are two principles used in the measurement of size of these particles :

(*a*) Two-dimensional visible comparison with standard circles under magnification by microscope
(*b*) Measurement of the free falling speed, or some related property, of the particles under controlled conditions.

Each system has considerable disadvantages. The microscope method involves the counting and comparison of a very large number of individual particles selected from a much larger gross sample. The results are very dependent upon the observer's skill, and the two-dimensional view of the particles does not always indicate true average shape of surface of the particles ; also the results of microscopic examination, although representing a true 'size,' are not always easily related to the actual behaviour of fine particles in a fluid.

Measurement of free falling speed, however, is directly related to the behaviour of the dust particles and, as such, provides a truer measure of the separation characteristics of a particle. The results, however, are expressed as ' particles having a free falling speed of *x* ft./sec. in a given liquid,' which can only be translated into a ' supposed ' size of particles if the densities of both particle and liquid are known. The nomenclature of particle ' size ' wholly in terms of free falling speed is not generally used ; such a system of nomenclature condenses very many different factors into a simple parameter, *e.g.*, shape of surface, density (of solid and liquid), interfacial forces, area of surface, etc., each of which may vary between wide limits.

Free falling speed may be measured by the sedimentometer, by which the dust particles are allowed to fall through a liquid of suitable density ; the deposited sediment is collected and weighed after successive intervals of time. Air elutriators may also be used, in which the sample of dust is agitated in a vertical upward column of air of known velocity ; particles of less than a certain free falling speed are thus caused to rise by separation from the remainder of the sample. Some designs of elutriator, notably the Haltain Infrasizer, use a succession of air columns with decreasing air velocities to obtain a series of group separations according to free falling speed.

These methods are too complex and too specialized to be used in routine laboratory analysis. Very few laboratories are equipped to provide such size analysis although the absence of information on particle characteristics is a serious handicap to the analysis of plant performance.

Stairmand[17] has compared the results obtained using various methods of sub-sieve size analysis, and his figures clearly show the difficulties of obtaining an accurate and consistent size analysis.

A simple system for distinguishing effective particle size is therefore required for routine purposes. Such a system need not give absolute sizes, as division of particles into approximate size ranges could be a most useful first step.

The Chemical Defence Research Experimental Station at Porton has developed an instrument called the Conifuge, in which large centrifugal acceleration is imparted to fine particles in a gas stream and by which they are settled on a glass slide on the periphery of the centrifuge. Their position on the slide, which can be removed and examined, is a direct measure of their free falling speed under a known accelerating force. The density of particle deposition on different parts of the slide can therefore be used as a guide to the concentration of the various ' effective ' sizes of the particles. This system has been developed to measure the size of liquid particles, but it would appear that a similar mechanism might usefully be adopted for the estimation of solid particle size and concentration, particularly at the low concentrations found in semi-cleaned blast-furnace gas.

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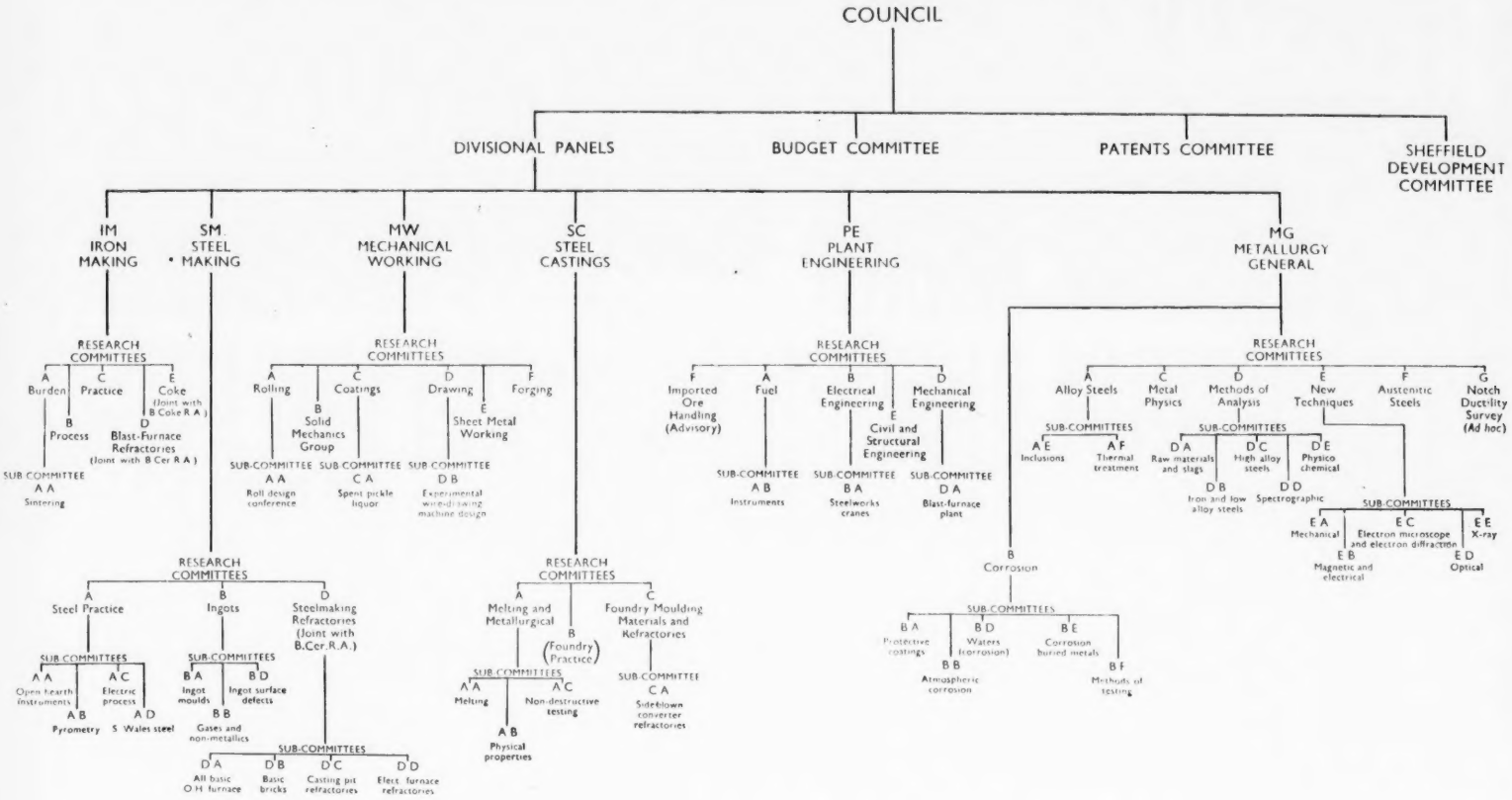

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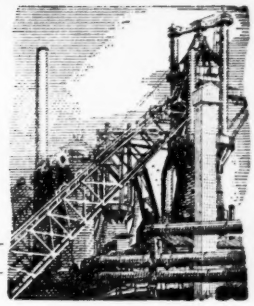

Electric Generation and Distribution in Iron and Steel Works

By T. Coxon

SYNOPSIS

The subject of electric generation and distribution in iron and steel works is reviewed, and particular attention is given to the completely integrated works of The Consett Iron Co., Ltd., at which many new projects are under way, involving complex internal changes and modified electrical requirements.

In discussing boilers, turbines, turbo-generators, and switchgear, examples of recent installations at these works are described, together with the reasons for their selection.

The factors governing the choice of voltage at which to generate and distribute are enumerated, and various transmission systems for main power distribution at 3·3, 6·6, or 11 kv. are described.

THE iron and steel industry in Great Britain includes plants of such wide diversity in type, size, and layout, that no two units have identical features. This nonconformity is emphasized when studied from the aspect of age. In the years subsequent to the two world wars many schemes of reconstruction have been, and are being, carried out, involving complex internal changes. In addition, large projects for completely new works are under way. By virtue of their size and complexity, these projects provide the iron and steel works engineer, and particularly the electrical engineer, with problems which, although existing in the past, did not govern as rigidly as they do today the whole structure of the electrical system.

This paper deals with the completely integrated works (together with an associated brickworks) of The Consett Iron Co., Ltd., a site plan of which is shown in Fig. 1. This plant was built after the first world war and is now undergoing reconstruction, involving, at the same time, some completely new projects.

RECONSTRUCTION PLANS

Between 1920 and 1925 the whole of the plant, with the exception of the main steam drives for the blooming and section mills, and the blast-furnaces, was reconstructed. The generator capacity installed at the Consett station in 1925 was 7000 kW., additional power requirements being met from the grid through a 5000-kVA. sub-station; as the electrical load increased further the Consett system was connected to the company's local colliery grid system.

The present programme of reconstruction was commenced in 1940 and includes:

(1) Three 5000-ton/week blast-furnaces
(2) Two new turbo-blowers for the above and the reconditioning of one existing turbo-blower
(3) An additional coke-oven battery, comprising 56 Becker ovens
(4) Coal- and coke-handling plants
(5) Conversion of existing 75-ton open-hearth furnaces to 150 tons capacity
(6) New rolling mills.

As part of the general programme of reconstruction at the blast-furnace plant, the existing battery of 160-lb. sq. in. boilers, some of which had been in commission for nearly 40 years, was scheduled for demolition; and because new and larger turbo-blowers were required consideration was given to establishing a central station designed to meet the steam and cold-blast requirements of the whole works.

As this scheme developed, and as it became possible to foresee all projects under way and subsequent developments, it was obvious that the existing system of electric power generation, together with its associated switchgear, would be inadequate to meet the new demands; also, to permit the commencement of site work on the new projects a partial reorganization of the electrical distribution system was necessary.

The old system of generation (like many others both in this country and in the U.S.A.) consisted of several generators of moderate capacity, *i.e.*, 1000–4000 kW., in stations in or near the iron and steel plant. A survey made in 1947 of generating plants in iron and steel works power stations showed that, of the 81 turbo-generators installed during the previous years, only five exceeded 10,000 kW. capacity; the average was 3110 kW., and the unit which represented more truly the size installed in these stations was 2250 kW.

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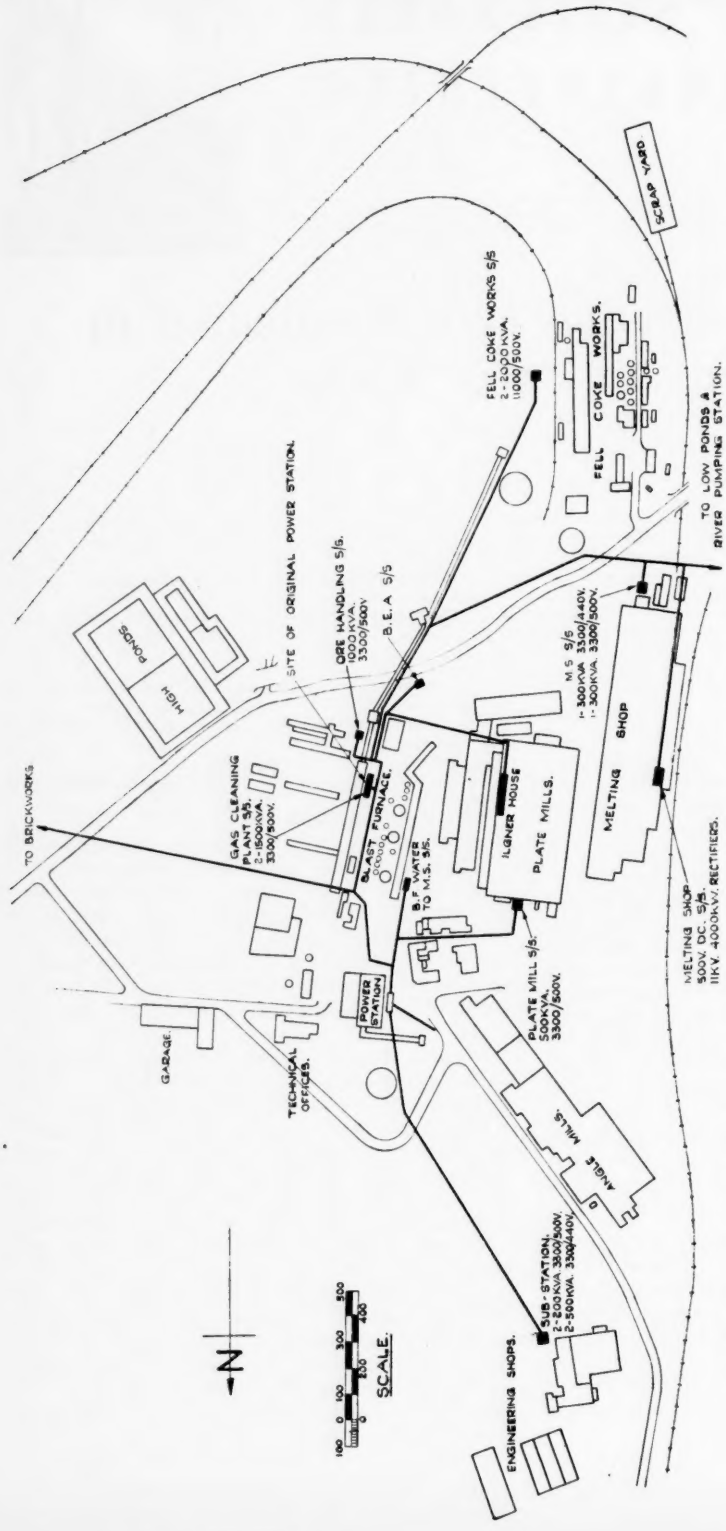


Fig. 1.—Plan of works site, showing route of H.T. trunk feeders from power station to sub-stations and main switching stations

With the vesting in the National Coal Board of the collieries' distribution network and power stations, the plant at Consett was left with two 3000-kW., 3·3-kV. sets, and one 1000-kW., 500-V., D.C. set. It was necessary to import from the National Coal Board and from the British Electricity Authority to meet normal requirements. Further, these sets, which have been in operation some 25 years, are, by modern standards, extremely inefficient; they take approximately 20 lb. of steam per kilowatt-hour at 160 lb./sq. in., 500° F., 1273 B.Th.U./lb., *i.e.*, a total of 25,460 B.Th.U./kWh. Modern sets of medium size (10–15 MW.), operating under more favourable steam conditions, have a consumption of just over 10 lb./kWh., when straight-condensing at 400 lb. sq. in., 750° F., 1390 B.Th.U. lb., *i.e.*, a total of 13,900 B.Th.U. kWh.; it was obvious, therefore, that power costs on a fuel basis could be nearly halved.

BOILERS

On an integrated plant, where there is available at all times a considerable quantity of surplus gas (coke-oven and blast-furnace), the selection of the type of boiler, and in particular of the firing arrangements, requires careful consideration. The complexity of the problem is increased by the fact that there is considerable diversity in the availability of the gases. The boilers must be arranged to operate basically on solid fuels (coal and coke breeze) with provision for burning, as and when available, all surplus gases (coke-oven and blast-furnace). Thus, the boiler plant is, in effect, designed as a compensator on the gaseous system.

Space does not permit a detailed exposition of the relative advantages of the numerous types of boilers, the widely varying arrangements of firing, and the

extensive range of fuels. It is sufficient to state that the five boilers which were considered to be best for the particular conditions, were as follows :

(*a*) Three Clarke Chapman tridrum-type boilers arranged for burning pulverized fuel, blast-furnace gas, and coke-oven gas, singly or in any mixture. Each of these three boilers has a rating of 90,000 lb. hr. of steam on blast-furnace gas only, and 120,000 lb. hr. on pulverized coal only. On mixtures of gas and solid fuels, any rating between these figures is obtained by reducing the quantity of gaseous fuel burned. Coke-oven gas can be burned in each of the units to produce 45,000 lb. hr. This gas is used only after all other requirements in the works have been satisfied.

(*b*) Two Clarke Chapman tridrum-type boilers, fitted with retort stokers to burn any inferior solid fuels available, one of them having burners for blast-furnace gas in addition. Each unit has a capacity of 120,000 lb. hr. on coal and coke breeze mixture, and 45,000 lb. on blast-furnace gas only.

Detailed information relating to the selection of boiler plant for steelworks is available in recent publications.[1, 2]

STEAM PRESSURE AND TEMPERATURE

It will be appreciated, when contemplating any central power station such as the Consett project, that in addition to the attention given to the problem of available fuels, the question of fuel or heat efficiency must be regarded as of equal importance.

Owing to the appreciable reduction in overall heat rates with high steam pressures and temperatures, there is today a great tendency to think only in terms of very high pressures and temperatures and to regard anything below 800 lb. sq. in. and 850–900° F. as old-fashioned and inefficient. It should, however, be borne in mind that, while pressures and temperatures of this order, and above, may be eminently suitable for the very large sets in B.E.A. stations, such conditions are not ideal for the moderate and smaller-sized turbo-generators and turbo-blowers usually associated with iron and steel works. Also, it must be remembered that, as recently as 1939, the Central Electricity Board had, for the major stations, established 600 lb. sq. in. at 800° F. as standard. Today, ten years later, the standard is 900 lb. sq. in. at 900° F., and a pressure of 1500 lb. sq. in. at 1050° F. is being considered for new base stations.

Further, in the case of blowing plant, the utmost reliability is essential, as it is not feasible to interconnect blowing stations as with electrical plant.

Since the total generating capacity to meet the immediate requirements was some 20 MW., and the probable size of units was 10–15 MW., steam pressures of 400–600 lb. sq. in. were under review. While optimum steam conditions for generating sets of this size may be taken as 600 lb./sq. in. at 825° F., for the turbo-blowers with an equivalent rating of 4000–5000 kW., optimum conditions were found to be about 400 lb. sq. in. at 750° F.

The figures given below, together with the curves in Fig. 2, show that an extra outlay of 10% in capital expenditure for the 600 lb. sq in., 825° F., condition, gives an improvement in efficiency of only

$$\frac{13{,}200 - 12{,}500}{13{,}200} \times 100 = 5{\cdot}3\%$$

Approximate Cost of Generating Plant as a Function of Steam Pressure and Temperature

Steam Pressure, lb. sq. in.	Steam Temperature, °F.	Approximate Cost, %
400–450	750	100
600	825	110
850	900	118
1250	950	125

For a base-load station operating with a high load factor and using entirely imported fuel, such a saving, particularly with present coal prices, would amply justify the additional 10% in capital cost. For a power station on an integrated plant, however, where the load factor is lower owing to the considerable diversity between the peak demands of the mills, and where a considerable proportion of the fuel is available internally in the form of surplus gas and coke breeze, such extra capital outlay would not be justified, even assuming all other factors, *i.e.*, reliability, availability, etc., to be equal. Also, provided that the works requirements are met, in a plant of moderate efficiency and cost, a high-efficiency plant is not economic as there is no reasonable market for surplus power.

The problems and economics of high-pressure steam for iron and steel works power have recently been reviewed and discussed.[3–6]

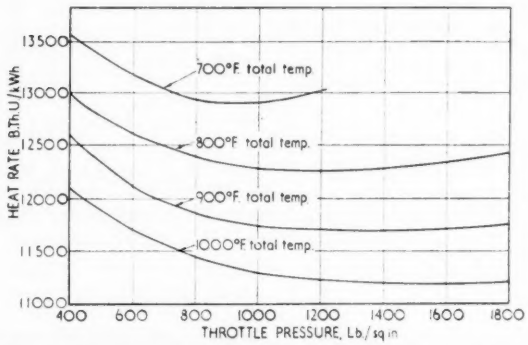


Fig. 2—Practical overall heat rates for fixed conditions of pressure and temperature

TURBINES

In addition to the steam requirements for electric power and turbo-blowers, large quantities of medium-pressure steam (160 lb. sq. in., 550° F.) are required for the steam-driven section mills, the coke-oven plant, and the plate mill shears, etc. With steam conditions at 400 lb. sq. in. and 750° F., consideration was given to extraction or exhaust-type turbines which would cater for the medium-pressure steam requirements and would give a great measure of economy. Since the steam-driven section mills would become redundant in a few years' time, and the medium-pressure steam requirements would be considerably reduced, exhaust-type turbines were eliminated.

The size and operating conditions of the turbo-blowers precluded these units from consideration

as extraction-type machines. On the other hand, the installation of some 30-MW. generating capacity to meet immediate requirements presented an excellent opportunity for such an application. Because of the reduced demand for medium-pressure steam in the near future, extraction-type turbines were decided upon, with condensers designed to give the full load of the turbine with a much reduced pass-out quantity.

When the approximate ultimate demands had been fixed and a reasonably accurate assessment of the immediate future requirements had been made, it was necessary to determine the number and size of the turbo-generators which would meet these demands, and to install the sets in an order that would meet present and immediate future requirements and that would provide at all stages the flexibility of operation so vitally necessary.

At the same time the question of the link-up with the B.E.A. was fully explored. The existing B.E.A. connection of 5000 kVA. had, at the time of the first reconstruction (1923), represented 50% of the works average demand; in 1944, it was approximately 30%, and in the future it will be only 10–15%; thus in its present form it is not of major importance. The installation of sets of a type and size to suit all stages of development of an interconnected B.E.A. station presents no difficulties; for steelworks plant, however, certain peculiarities of the electric load and of the power distribution system make the problem complex. These peculiarities are briefly as follows:

(*a*) A major portion of the load consists of very large induction and synchronous motors, each of several thousand horse-power

(*b*) Major portions of the load are concentrated in relatively few centres, such as the mill motor rooms

(*c*) The loads imposed by the mills are of a highly fluctuating nature, varying (in a matter of seconds) from practically zero with the mill idling, to peaks of several thousand horse-power when rolling; further, the load curves for mills differ very considerably with the type of mill, *i.e.*, reversing, slabbing, blooming, and plate mills, and with continuous-strip and billet mills.

These anomalies introduce problems in the design of the power, general conversion, and distributing systems which are not found in any other industry. Two additional factors which are, or should be, the criterion of assessment of all steelworks plant, electrical or otherwise, are reliability and availability. These terms are defined as follows:

Reliability—A quality denoted by intrinsic strength, robustness, and those features which ensure correct and continuous functioning during the whole period of availability (defined below). This must apply to all items of steelworks plant.

Availability—This quality is defined as 'functional'; it could be expressed as the relation between the maximum hours for which the plant or unit can remain in operation when working to, or within, the specified conditions, and the total hours of the period under consideration. This ratio is usually expressed as percentage availability per annum. For cables, transformers, generators, switchgear, etc., it is practically 100% and, within limits, is independent of the type and make of equipment. On the other hand, for turbines, with their condensers, boilers, cooling towers, etc., the factor is affected by the necessary periodic shutdowns for inspection, cleaning, etc., and is not independent of the type and make of equipment.

This interpretation is for the ideal or maximum availability possible, but in practice it is affected by breakdowns, etc., and hence is also a function of reliability.

TURBO-GENERATORS

After much deliberation on all the aspects of capital expenditure, efficiency, reliability, and availability, two 15,000-kW. Parsons turbo-generator sets were installed in the first instance. The turbines were to be suitable for 400 lb./sq. in. (gauge) and 770° F., and were designed to handle 80,000 lb. of pass-out steam per hour, at 165 lb./sq. in. at 585° F. The saving effected by the pass-out-type turbine is illustrated by the following comparison of performance of a straight-condensing set and a pass-out set:

Straight-Condensing, 15-MW. Set

Steam consumption	= 10·2 lb./kWh.
Steam pressure	= 400 lb./sq. in. (gauge)
Steam temperature	= 750° F.
Total heat of steam	= 1390 B.Th.U./lb.
Temp. of condensate leaving feed-heating system	= 230° F.
Heat at stop valve	$= 10{\cdot}2 \times 15{,}000 \times 1390$ $= 212{\cdot}5 \times 10^6$ B.Th.U.
Heat returned in condensate	$= 10{\cdot}2 \times 15{,}000 \times (230-32)$ $= 30{\cdot}3 \times 10^6$ B.Th.U.
Total heat consumption	$= 182{\cdot}2 \times 10^6$ B.Th.U. $= \frac{182{\cdot}2 \times 10^6}{15{,}000}$ = 12,130 B.Th.U./kWh.
Efficiency	$= \frac{3416}{12{,}130} \times 100 = 28{\cdot}1\%$

Pass-Out, 15-MW. Set

Steam at stop valve	= 217,000 lb./hr.
Steam pressure	= 400 lb./sq. in. (gauge)
Steam temperature	= 750° F.
Total heat of steam	= 1390 B.Th.U./lb.
Steam at pass-out	= 80,000 lb./hr. at 160 lb./sq. in. (gauge), 580° F.
Heat in steam at pass-out	= 1314 B.Th.U./lb.
Temp. of water leaving feed-heating system	= 230° F.
Heat in steam at stop valve	$= 217{,}000 \times 1390$ $= 302 \times 10^6$ B.Th.U.
Heat in steam at pass-out	$= 80{,}000 \times 1314$ $= 107{\cdot}2 \times 10^6$ B.Th.U.
Heat returned in condensate	$= (217{,}000-80{,}000) \times (230-32)$ $= 27{\cdot}1 \times 10^6$ B.Th.U.
Total heat consumption	$= (302 \times 10^6) - [(107{\cdot}2 \times 10^6) + (27{\cdot}1 \times 10^6)]$ $= 167{\cdot}7 \times 10^6$ B.Th.U. $= \frac{167{\cdot}7 \times 10^6}{15{,}000}$ = 11,200 B.Th.U./kWh.

Efficiency	$= \frac{3416}{11{,}200} \times 100$
	$= 30{\cdot}6\%$
Improvement in efficiency over straight-condensing type	$= 9\%$

The station, boiler house, and all ancillary equipment, including the main and auxiliary switchgear, have been designed for the addition, in the immediate future, of a third 15-MW. set, the whole to form a unit to which further similar gear could be added if necessary.

Having agreed upon the number and size of the turbo-generators and the steaming conditions for the station, the most important questions of station economics were covered. There remained, however, the important problem of the selection of electrical equipment to ensure that the station auxiliaries and ancillary gear would be sufficiently flexible and reliable so that (*a*) at all times plant would be available with sufficient capacity to meet the works demands, (*b*) provision was made for reasonable standby, and (*c*) there would be the highest degree of reliability and flexibility in the main control and distributing system.

SWITCHGEAR

Of all the forms of electrical equipment which can contribute to the reliability and flexibility of operation of a generating station and also determine the extent to which the generator capacity can be increased, switchgear is undoubtedly the most important. This is particularly so in the case of iron and steel works stations where the peculiar nature of the load makes it desirable to have all or most of the generator capacity connected in parallel on the busbars, so minimizing the effect of fluctuations of load.

Turbo-generators must, of course, be sufficiently robust to withstand the maximum electrical and mechanical stresses which they may be called upon to handle ; these stresses are a function of the characteristics of the individual generators and are independent of the number and size of other generators and synchronous machines coupled to the same busbars.

On the other hand, switchgear has to deal adequately with the combined electrical and mechanical stresses set up by the maximum power which can be fed into the system by all connected generators and synchronous machines under the worst fault conditions ; since the purpose of switchgear is to safeguard, as well as to control, all other electrical apparatus, every part must be designed to function efficiently at all times.

Again, switchgear is the only form of electrical equipment which, during abnormal conditions, is called upon to take an active rôle from the inception of a fault to the restoration of normal and healthy conditions. These duties include discrimination (whereby only those units controlling the faulty section or sections of the plant will be affected), rapid isolation of such units from the system (so ensuring the continuity of supply by limiting both the area affected and the damage done by the fault current) and, at the same time, under certain circumstances, the dissipation of practically the full energy of the fault.

Furthermore, after a circuit breaker has operated on a fault up to 100% of its breaking-capacity rating, the mechanical parts and insulators of the circuit breaker should be essentially in the same condition as before the occurrence of the fault. The circuit breaker has to be capable of being put back into service immediately, and of making, carrying, and breaking its rated normal current at the rated service voltage without any adjustments or renewals. The only renewals required to restore the unit to full making and breaking capacity, respectively, are arcing tips, and oil for oil circuit breakers,

Under similar conditions the rôle taken by generators, motors, transformers, cables, etc., can be regarded as passive, and whilst such equipment may be constructed to withstand the several stresses set up, the switchgear must take control and operate with such rapidity that no permanent damage can be sustained.

The selection of suitably rated switchgear is, therefore, of primary importance and can be made only with a full knowledge of load conditions and short-circuit conditions.

The type of switchgear and the voltage of the system most suited to a particular installation can best be determined from operational and economic standpoints when this information is available.

Short-Circuit Calculations

Much detailed and valuable information relating to the calculation of short-circuit currents and to the selection of circuit breakers is given in B.S.S. 116, Parts I and II, and in numerous publications.[7–11]

The source of fault power originates in the connected generating plant and in synchronous machines normally taking power from the system, but which may feed power into the system under fault conditions. The value of fault current which can flow in any network under fault conditions is limited only by the impedance in that system contributed by transformers, cables, reactors, etc. Hence, it is essential to have (i) full details of all plant that can feed into a fault, and (ii) full details of all factors that can limit the fault.

For simplicity and ease of rapid calculation, the percentage-reactance method of calculating fault currents is normally used, since it dispenses with the vectorial additions of the resistance and reactance components by ignoring resistance entirely and so gives current values on the safe side, *i.e.*, slightly higher than if the resistance component were taken into account. In the case of cable or overhead line feeders, resistance may be of such magnitude that its effect is considerable. This is particulary so in lower voltage calculations and is dealt with later. In general, when the reactance exceeds three times the resistance, the latter may be ignored and the resultant calculations will not be in error by more than 5%.

The fault MVA. of a network is calculated from the instantaneous symmetrical short-circuit current and depends mainly upon the generator reactance. In British practice this is the basis for the selection of switchgear.

A synchronous machine has many different reactances. In calculating system symmetrical fault currents, only three of these reactances, *i.e.*, sub-transient, transient, and synchronous, are used. Definitions of these reactances are given below :

Sub-Transient Reactance is the rapidly decreasing or decaying component, lasting for only a few cycles.

Transient Reactance is a slowly decaying component lasting for 10–100 cycles.

Synchronous Reactance is a sustained component and is the reactance value obtained from the short-circuit curve for the machine.

To calculate the symmetrical short-circuit current the sub-transient reactance value is used ; also since the plant (comprising generators and all other synchronous machines) associated with any steelworks system will almost always have different kVA. ratings, the calculation can be simplified by converting all reactances to a common base. The value of this base is unimportant, but the author has found 100 MVA. to be most convenient. With this method, no account need be taken of the voltage ; thus, in the case so far presented there has been no occasion to consider voltage.

The conversion of the reactance percentage on the kVA. rating to the reactance percentage on the 100-MVA. basis is made as follows :

$$\text{Base reactance, } \% = \frac{\text{Base kVA.}}{\text{Plant kVA.}} \times \text{plant reactance, } \%$$

Thus, for a 10,000-kVA. transformer or synchronous machine having a reactance of 10%,

$$\text{Base reactance, } \% = \frac{100{,}000}{10{,}000} \times 10 = 100\%$$

The calculation of system short circuits, for any but the simplest of networks, is laborious. While space limitations do not permit full calculations for the various schemes and arrangements reviewed, the subject is considered to be of sufficient importance to justify the inclusion of the short-circuit calculations on the final system set up which determined the breaking capacity of the switchgear installed.

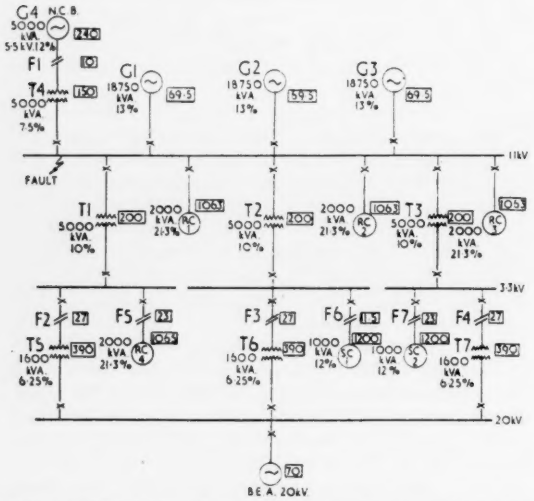


Fig. 3—Short-circuit calculations. Basic schematic diagram

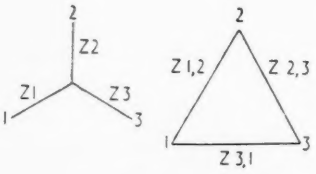
Figure 3 shows the final arrangement of the network with three 15-MW. generators connected in parallel to the busbars. The ratings, with the corresponding reactance values, for all synchronous machines and transformers, are given. The framed figure beside each item is the reactance value to a 100-MVA. base. In the case of feeders, only the equivalent reactance values are shown.

Figure 4 shows the method of resolving reactance networks into progressively simpler equivalent arrangements until the resultant reactance of the whole network is obtained.

The network diagram in Fig. 4.1 is for a fault on the main generator busbars or on any outgoing feeder at a point close to the busbars. While the reactances of the generators, rotary convertors, and the N.C.B., can readily be resolved into a single equivalent value, the section concerned with the feeding into the fault through cascade transformers by the B.E.A. is complicated by the fact that synchronous condensers and a rotary convertor contribute at intermediate points.

The process of simplification is aided considerably by the use of two three-terminal reactance transformations known as the star/delta and delta/star transformations. In these, any group of three vector reactances connected in star may be replaced by an equivalent delta-connected group, or *vice versa*.

Given the values $Z1$, $Z2$, $Z3$, in a star-connected group, the equivalent delta values are determined as follows :

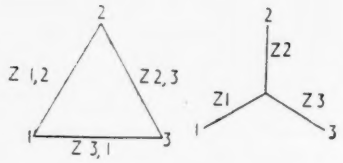


$$Z1,2 = Z1 + Z2 + \frac{Z1.Z2}{Z3}$$

$$Z2,3 = Z2 + Z3 + \frac{Z2.Z3}{Z1}$$

$$Z3,1 = Z3 + Z1 + \frac{Z3 + Z1}{Z2}$$

Similarly, given the values $Z1,2$, $Z2,3$, and $Z3,1$ in a delta-connected group, the equivalent star values $Z1$, $Z2$, and $Z3$, are :



$$Z1 = \frac{Z1,2.Z3,1}{Z1,2 + Z2,3 + Z3,1}$$

$$Z2 = \frac{Z1.2.Z2,3}{Z1,2 + Z1.3 + Z3,1}$$

$$Z3 = \frac{Z3,1.Z2,3}{Z1,2 + Z2,3 + Z3,1}$$

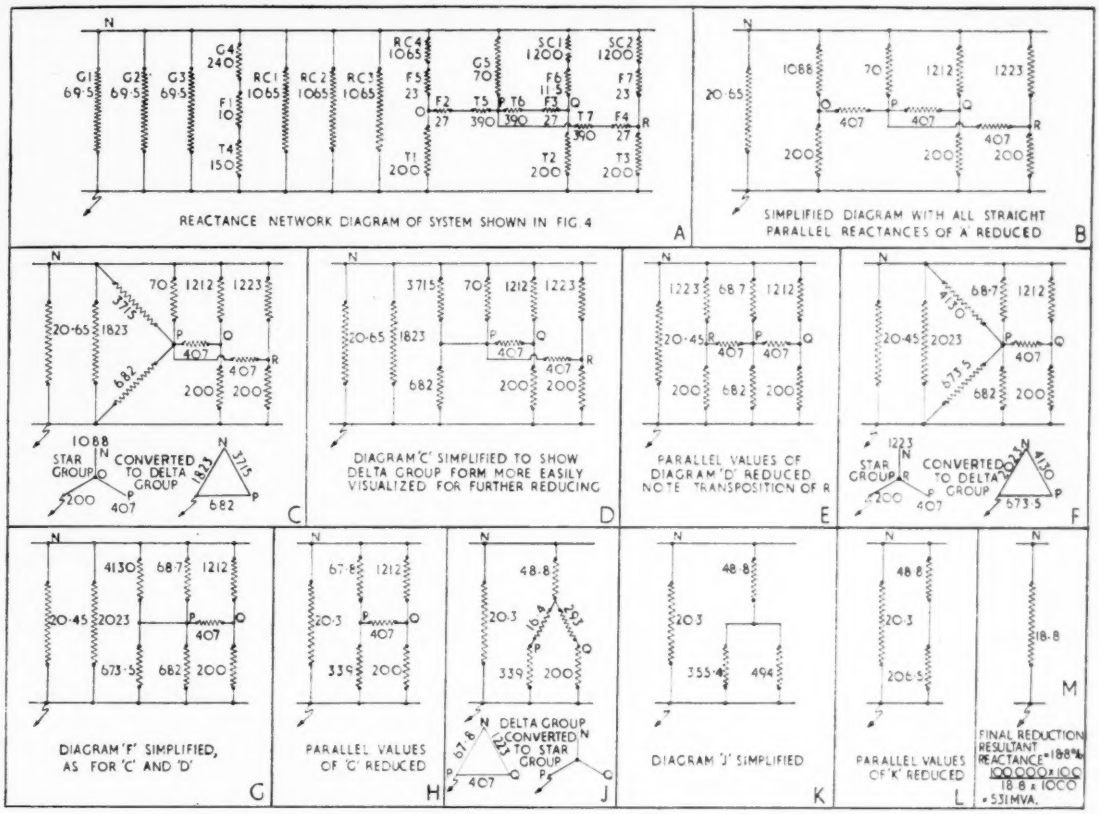

REACTANCE NETWORK DIAGRAM OF SYSTEM SHOWN IN FIG. 4
A
SIMPLIFIED DIAGRAM WITH ALL STRAIGHT PARALLEL REACTANCES OF A REDUCED
B
STAR GROUP CONVERTED TO DELTA GROUP
C
DIAGRAM 'C' SIMPLIFIED TO SHOW DELTA GROUP FORM MORE EASILY VISUALIZED FOR FURTHER REDUCING
D
PARALLEL VALUES OF DIAGRAM 'D' REDUCED. NOTE TRANSPOSITION OF R
E
STAR GROUP CONVERTED TO DELTA GROUP
F
DIAGRAM 'F' SIMPLIFIED, AS FOR 'C' AND 'D'
G
PARALLEL VALUES OF 'G' REDUCED
H
DELTA GROUP CONVERTED TO STAR GROUP
J
DIAGRAM 'J' SIMPLIFIED
K
PARALLEL VALUES OF 'K' REDUCED
L
FINAL REDUCTION
M

Fig. 4—Short-circuit calculations. Reactance network diagrams

These expressions have been employed in the solution of Fig. 4. Thus, for the arrangement shown in Fig. 3 the short-circuit or fault capacity of the system is :

$$\frac{100{,}000 \times 100}{18{\cdot}8} = 531{,}000 \text{ kVA.}$$

$$= 531 \text{ MVA.}$$

In this calculation no allowance has been made for interconnecting cables between generators and circuit breakers or for busbars. Thus, whatever arrangement of switchgear, or of switchgear and reactors, is selected it is safe to take 500 MVA. as a basis.

Switchgear of any make and type for 500-MVA. breaking capacity is costly. Assuming that there will be no generator or synchronous capacity additional to the above, the arrangement can be modified by introducing busbar or tie-bar reactors to limit the fault MVA. on any part of the main switchgear to, say, 375 MVA., the next standard lower. On the other hand, if there is any reasonable possibility of additional generator capacity being required in the future, whether it be in 5, 10, or 15 years—and in an iron and steel plant he would be a very imprudent man who would discount with conviction such a potentiality—it is politic to adopt the MVA. rating indicated, but to provide facilities for the inclusion of reactors designed to keep the system short-circuit current within the switchgear rating.

At Consett 500-MVA. switchgear (Fig. 5) has been installed : it is capable of handling a total connected fault capacity of 750 MVA., but is limited to 500 MVA. at all points by tie-bar reactors. The arrangement is shown in Fig. 6. To explore the whole of this network with a view to ascertaining the fault currents

Fig. 5—11-kV., 500-MVA. switchgear

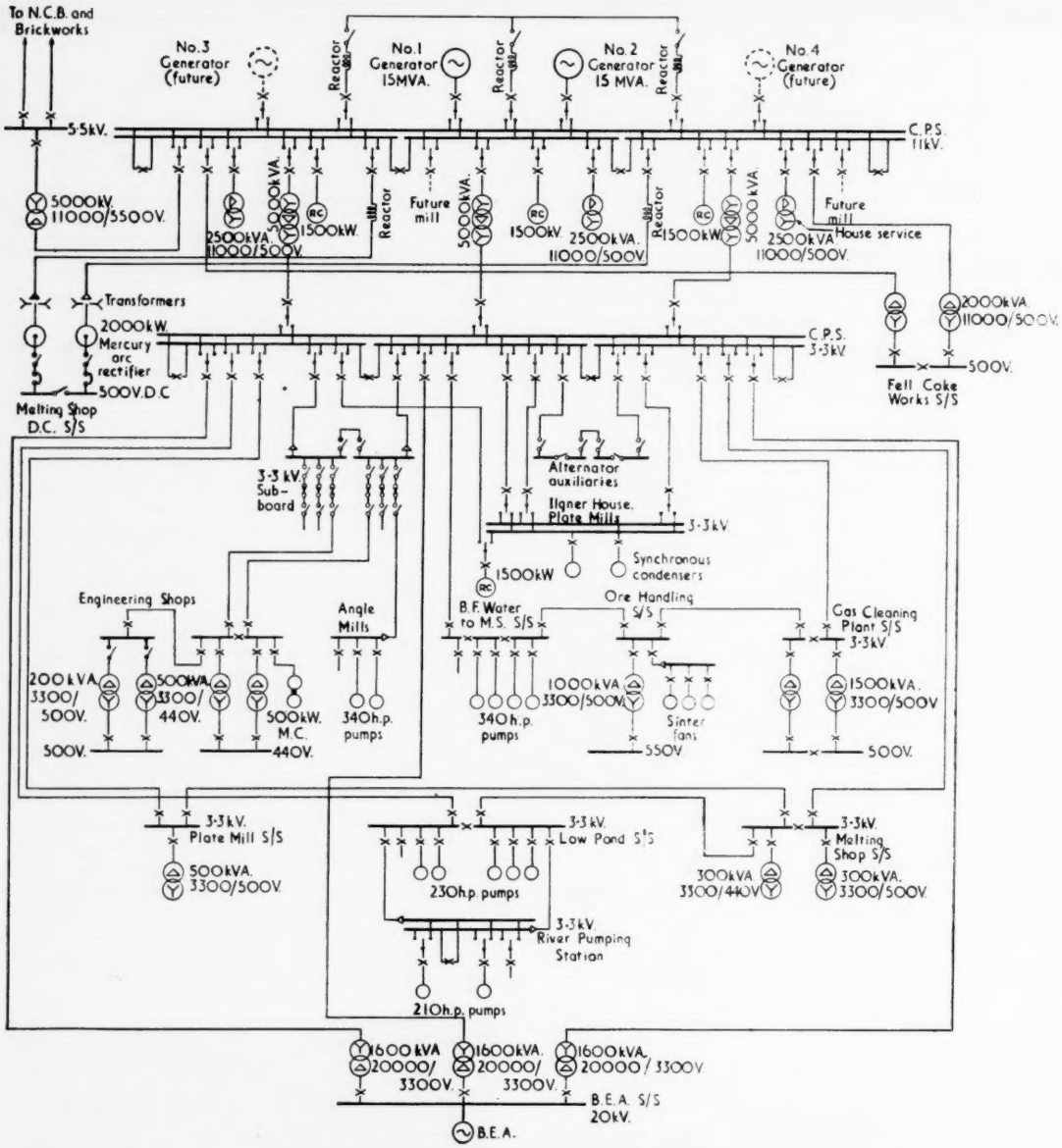


Fig. 6—Schematic diagram of complete system

at various points of the system, *e.g.*, 3·3-kV. and 5·5-kV. busbars and/or feeders, or for determining the effects of alternative schemes and extensions, is so unwieldy an operation that recourse is made to short-circuit calculators or network analysers, as employed by switchgear manufacturers.

Selection of Switchgear

In an integrated iron and steel plant with a radius of approximately one mile for main power feeders, the question of transmission at very high voltages does not arise. Also, at Consett, there is no need to consider switchgear of types associated with special applications such as electric smelting furnaces.

Standard oil circuit breakers of the horizontal draw-out, metalclad, compound-insulated type appear to be most suited to steelworks requirements. The chief advantages of this gear are :

(1) Greater safety to life

(2) Saving of space

(3) Reduced hazard from fire, busbar faults, etc.

(4) Exclusion of dust, and freedom from corrosion of current-carrying parts, permitting the circuit breaker to remain in service, subject to normal operation, for long periods without attention.

Table I

COMPARISON OF COST OF SWITCHGEAR AT 6·6, 11, AND 22 kV.

Switches		Units of Cost					
		6·6 kV.		11 kV.		22 kV.	
Unit	No.	Switch Rating	Cost	Switch Rating	Cost	Switch Rating	Cost
Generator	3	2000	144	1200	96	800	114
Bus-section	1	2000	48	1200	32	800	38
Coupler	2	2000	96	1200	72	800	76
Feeder	10	1200	350	800	280	400	340
Total cost	...	...	638	...	480	...	568

This type of equipment is available in the voltage range likely to be required by iron and steelworks.

CHOICE OF VOLTAGE

The choice of voltage at which to generate and to distribute on the main system is determined by three factors, *viz.*, switchgear, cables, and generators.

Switchgear

The size of generator (15 MW., 18·75 MVA.) and the fault capacity of the system (500 MVA.) having been fixed, it is necessary to assess the maximum load to be handled by any individual circuit breaker. For the station under consideration this figure was 10 MVA. Reference to B.S.S. 116, Part I, shows that standard 3·3-kV. gear is debarred from consideration, being available only up to 1200 amp. (6950 kVA. at 250 MVA.) rating of breaking capacity.

The economics of switchgear selection, when considering voltage, rating, and MVA. breaking capacity, are given in Table I, and curves showing the relation between the cost and current rating for different values of fault capacities (250–750 MVA.) and voltages (6·6, 11·0, and 22 kV.) are shown in Fig. 7. These curves are for duplicate busbar, metalclad, compound-filled, horizontal draw-out, oil circuit breakers. The kVA. capacity of the standard range of 500-MVA. switchgear at 6·6, 11, and 22 kV. (B.S.S. 116, Part I), is as follows :

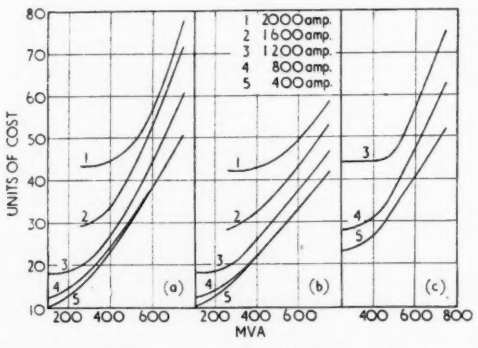


Fig 7—Cost/MVA. curves for oil-immersed metalclad switchgear at standard voltages and currents to B.S.S. 116, part I : (*a*) 6·6 kV., (*b*) 11 kV., (*c*) 22 kV.

Circuit Breaker Rating	kVA. Capacity of Standard 500-MVA. Switchgear at :		
	6·6 kV.	11 kV.	22 kV.
400	4600	7600	15,200
800	9200	15,200	30,400
1200	13,800	22,800	45,600
1600	18,400	30,400	
2000	23,000	38,000	

For comparison of costs, an estimate was made for a switchboard comprising :

Three generator switches each rated at 18,750 kVA.
Ten feeder switches each rated at 10,000 kVA.
One bus-section switch rated at 18,750 kVA.
Two bus-coupler switches each rated at 18,750 kVA.

From Table I, which gives the comparative costs, taken from Fig. 7, it will be seen that the optimum voltage for the main switchgear is 11 kV. It is interesting to observe, however, that the 6·6-kV. gear is 13% higher in cost than the 22-kV. equipment. This is accounted for by the greater mechanical robustness of the 6·6-kV. switchgear owing to the exceptionally heavy currents associated with the lower voltage.

Cables

The selection of the optimum voltage for the main cable installation cannot be decided from economical considerations based on Kelvin's law, the chief factor of which is the relation between the annual cost of energy wasted and the interest on capital.

The arrangement of feeders most suited to iron and steel works is a network of duplicate and/or alternative feeders with all feeders usefully employed (*see* Distribution). Thus, capital costs, compared with a single feeder network, to which Kelvin's law is particularly applicable, are double and the annual losses are halved. The selection is made on capital cost and applicability.

A comparison of cable requirements for 6·6, 11, and 22-kV. systems, similar to that for switchgear, is given below :

Voltage, kV.	Current, amp.	Cables
18,750-kVA. Alternators		
6·6	1650	3/0·75 sq. in./phase
11	980	2/0·75 sq. in./phase
22	490	1/0·75 sq. in./phase
10,000-kVA. Feeders		
6·6	875	3/0·3 sq. in., 3-core
11	525	2/0·3 sq. in., 3-core
22	263	1/0·3 sq. in., 3-core

Cable sizes have been determined from the B.E.A.I.R.A. Tables (reference F/T.128), allowance being made for trefoil formation in ducts and for close grouping of 3-core cables in culverts. For a given cross-section the relative costs of cables are as follows :

6·6 kV. 1·0 ; 11 kV. 1·0 ; 22 kV. 1·8 ;

thus from Table I the comparative costs are :

6·6 kV. 3·0 ; 11 kV. 2·0 ; 22 kV. 1·8.

In steelplants there are, apart from sub-stations, no applications for which 22 kV. would be suitable ; on the other hand 11 kV. can be employed for all mill drives on, or above, 2500–3000 h.p.

As the costs of the 11- and 22-kV. systems are so close, the majority of steelworks electrical engineers will, from general considerations, *i.e.*, the extra skill required in handling, jointing, and installing 22-kV. cables, agree that the 11-kV. system is more suitable.

Generators

The high-speed turbo-alternator has reached that stage of development where there is little or nothing to chose between the products of different manufacturers. Limitations of design, chiefly mechanical, lay down the conditions for the rotor, and, depending upon size, for the method of ventilation (*i.e.*, self or forced, and latterly for very large sets, air or hydrogen). The arrangement of the stator end-windings permits some deviation ; this feature in turn is governed by the size of the set, and by the voltage.

While today, for large B.E.A. stations, there is a certain vogue for 33-kV. units, there is little doubt that the designer, given a free hand, would design a machine with the most convenient arrangement of windings, taking the voltage as it comes and connecting to the busbars through transformers. For medium-sized steelworks stations, such conditions do not exist. All requirements are met by sets at 11 kV. with simple, yet robust, basket-type end-windings and forced-air ventilation.

Summary

The foregoing analysis of the major factors governing the main system voltage shows that 11 kV. is the most suited for this particular station. Figure 6 gives details of the 11-kV. gear ; on-load busbar selection is provided on the generator circuit breakers. It is considered that the arrangement of duplicate busbars, one of which is sectionalized, with busbar couplers for each end-section, gives maximum flexibility.

The on-load bus-selectors, the section switches, and the bus-couplers, must be interlocked to ensure correct sequence of operation.

3·3-kV. SWITCHGEAR

Reference has already been made to the original 3·3-kV. system from which the whole of the iron and steel plant at Consett was serviced. At these works, as at so many other steelworks, the formidable problem of modifying or replacing existing switchgear by providing equipment of adequate breaking capacity has had to be faced. Some reference should be made to the inadequacy of switchgear supplied, as in the case of Consett, about 25 years ago.

At that time, although those concerned with the design of power-station equipment and switchgear in particular, were fully aware of the problem, little or no equipment was available with capacity sufficient to establish indisputably the breaking capacity of switchgear, and manufacturers had, in all good faith, assigned ratings to switchgear on a more or less empirical basis. With the advent of the necessary testing equipment, it was found that these ratings had been very optimistic, and type tests demonstrated that the actual breaking capacity of the gear was, in some cases, as low as 25–15% of the estimated rated capacity.

Fortunately, the occasions on which, in practice, a circuit breaker is called upon to handle maximum fault currents are rare, and the bulk of this over-rated switchgear has continued to give satisfactory and uninterrupted service. Where such circuit breakers have been called upon to function under most adverse conditions—this applies also to B.E.A. stations—the results have often been disastrous, causing considerable incidental damage and inestimable losses to industry.

The modification of switchgear, other than a complete rebuild at a cost nearly equal to new switchgear, implies providing back-up protection in the form of high-tension H.R.C. fuses with cut-off values below the maker's estimated breaking capacity of the old gear. These fuses have many useful applications, but they are not suitable for a steelplant trunk feeder.

New 3·3-kV. switchgear was installed in place of the obsolete units, since 3·3-kV. would be required for the larger of the power-station auxiliaries, and for pump motors, exciter sets, and Ward-Leonard sets in the new mills. Also, some of the present 3·3-kV. plant was to continue in operation in the final scheme. In addition, considerable savings could be effected by transferring the A.C.–D.C. conversion plant to the central station.

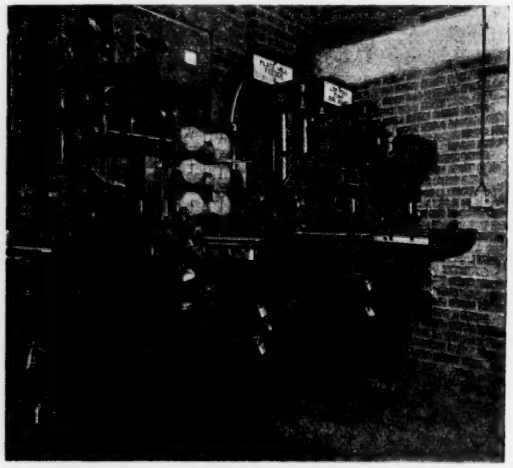


Fig. 8—3·3-kV. switchgear

The new gear, together with the A.C.–D.C. conversion plant and its associated D.C. switchgear, is accommodated in the power station, resulting in a compact, combined switchgear and distributing unit for the whole works (*see* Fig. 8).

Breaking Capacity—In the years subsequent to the 1923–25 reconstruction, extensions, and additions usual to a steelworks had been made. All the 3·3-kV. switchgear supplied after approximately 1935 was of 100 MVA. rating. From an economic viewpoint it was therefore most desirable to confine the replacement of 3·3-kV. gear to the power station and to arrange the system so that the fault capacity on any part of the 3·3-kV. network was limited to 100 MVA.

With a 3·3-kV. demand of 10,000–12,000 kVA., necessitating 15,000 kVA. of transformer capacity, and with the B.E.A. 3·3-kV. connections consisting of three 1600-kVA. transformers, a most convenient and flexible layout was obtained (*see* central power station, 3·3-kV., in Fig. 6).

To keep the MVA. rating to within 100 it is necessary to operate the central-power-station 3·3-kV. board as a three-section unit and to supervise and control all switching on the system network so that the sections do not become coupled together inadvertently through the bus-section switches and bus-couplers at the various switching stations. At the same time, full provision is made to ensure the maximum availability of supply to all feeders by the installation, at the central-power-station, of a duplicate busbar 3·3-kV. board, complete with bus-section switches and bus-couplers.

Figure 9, the reactance network diagram for a fault on one section of the main 3·3-kV. system, gives some indication of the complicated and tedious calculations involved. The diagram has been drawn for four generators with tie-bar reactors.

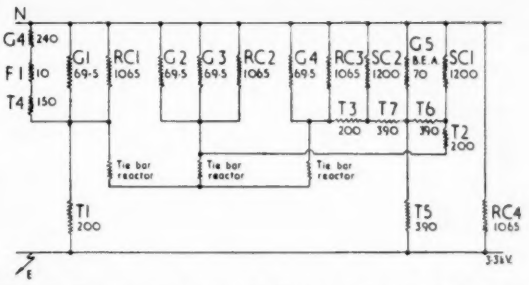


Fig. 9—Reactance network diagram for fault on one section of 3·3-kV. busbars, with four 15-MW. sets on the 11-kV. busbars with tie-bar reactors

THE DISTRIBUTION SYSTEM

In recent years all plant, and in particular steelworks plant, has become more and more dependent on electrical equipment, until, today, there is scarcely an item of equipment that does not rely directly or indirectly upon electric power. Thus the electrical network system, from power station to sub-station and to the main distributing points, must be as reliable and as flexible as the main and ancillary plant of the power station itself, providing at all times maximum availability of supply and facilities for extensions at all points.

With plants of moderate size, where the main power distribution is at 3·3, 6·6, or 11-kV., there very often exists a mixture of overhead and underground systems.

Overhead Transmission

The overhead system may consist of one or more of the following: Bare copper, V.I.R. insulated cable, or P.I.L.C.S.W.A. cable.

Bare Copper Transmission—While the general arrangements of integrated steelplants tend to conform to a standard pattern, there is considerable diversity in layout, and more particularly in the buildings of the individual units; it is, therefore, practically impossible to have what might be regarded as a first-class overhead system. Further, the area of the plant is generally too large for overhead transmission, with multitudinous terminals, outdoor isolating and sectionalizing switches, and protecting catenaries, to be an economical proposition. On the operational side there are numerous disadvantages, *e.g.*, the frequent cleaning of insulators, isolators, etc., and the susceptibility to involuntary tripouts caused by lightning.

V.I.R. Insulated Cables—All the factors mentioned for bare copper apply equally to V.I.R. overhead cables. There is, however, a tendency to run such cables racked too close to walls and roofs; also, V.I.R. insulation is not suitable for exposure to weather or to the sun; in a comparatively short time the insulation perishes and becomes brittle and may by contact or vibration break away and leave the copper bare.

The insulation on the conductor gives a false sense of security and encourages the taking of risks. With bare copper there is no question of whether or not it is safe to touch the conductors.

P.I.L.C.S.W.A. Cables—Paper-insulated, lead-covered, single-wire armoured cables provide a completely metal-enclosed power system. The useful life of this type of cable is rated higher than that of any other class of electrical apparatus. It is highly versatile in its application, whether underground, or overhead, or in water; it can be routed and installed in almost any way (if due regard is given to the rating and if the outer protection is selected for the particular conditions) without impairing its efficiency or reliability.

The choice of running overhead or underground in steelworks is conditioned by the individual plant. Invariably, in a works with a complete paper-insulated-cable distributing network most of the system is underground and the remainder is overhead or cleated to walls and buildings. Generally, P.I.L.C.S.W.A. cables are used overhead where steel structures, such as bridgework for steam and air services, follow the route.

The cost of overhead installation is similar to that for buried cables, but is slightly less than for cables cleated in concrete trenches or culverts. Unless the whole of the feeder is overhead (and this is seldom the case), there is little, if any, advantage in current rating. Furthermore, overhead cables may have to be derated because of high ambient temperatures and heating due to solar radiation.

Underground Transmission

For steelworks applications, at 3·3, 6·6, and 11 kV., the only satisfactory medium for transmission of electrical power is the paper-insulated cable, and, apart from single-core cables for three-phase working, this should be armoured with suitable outer protection.

The term 'underground' applies to cables which are (*a*) buried in the ground, (*b*) drawn into pipes or ducts, or (*c*) cleated or suspended on hangers in trenches or culverts.

Buried Cables—The greater proportion of high-tension, paper-insulated, lead-covered, armoured or unarmoured cable is laid direct in the ground, and much of this has given continuous trouble-free service over periods of 20, 30, and even 40 years. This method of transmitting electric power is most suitable where the erection of overhead lines is impracticable and/or where a high degree of reliability and availability of service is required. It is limited, however, to routes where the conditions of the soil (thermal resistivity and non-corrosive conditions) are satisfactory, where there is no possibility of land subsidence, and where the cables will always be accessible if breakdowns occur.

A further limitation is the number of feeders which can conveniently be grouped together in one cable route, because, as the number of cables in a route is increased there is a considerable increase in the space requirements and an unavoidable reduction in the cable capacity. Table II shows the space requirements for different arrangements of buried high-tension P.I.L.C.S.W.A. cables and also the corresponding rating factor. It will be seen that even for twelve feeders the space requirements are considerable and the rating is reduced by approximately 50%. Additional corrections may have to be made for a soil of high thermal resistivity.

Mention has already been made of the desirability of duplicate or alternative feeders; thus, in all the main groups of cables in the main distributing network only about 50% will be in operation at one time; or alternatively all cables may be in service but carrying only 50% of the normal full-load current. In practice, therefore, the actual derating will be very much less than that shown in Table II. Since further tables are available for the derating of cables in ducts and as the rating factor throughout is on the same basis, the figures can be taken as directly comparable.

In steelplants, where numerous cables spread out from the power station, the condition of the soil is often far from satisfactory (due to excessive dryness, slag tipping, etc.), and where it is important to provide facilities suitable for extensions, such an arrangement is totally unsuited. For one, two, or even three feeders to outlying units of plant, *e.g.*, brickworks or pump houses, where all the conditions are favourable, it is the most convenient arrangement. If the cables are grouped in more than one layer, it is often difficult or impossible, in case of breakdown, to obtain access to cables other than those in the top layer.

Cable Ducts or Pipes (Buried in the Ground)—The ducts in common use for electrical distributing work are single-way, three-way, six-way, and nine-way.

Single-way ducts can be arranged in the ground in the same grouping as for cables buried direct. Table II gives the space requirements and the rating factors for each type of duct. It will be seen that the cumbersome grouping of single-way ducts requires less space than the corresponding grouping of cables buried direct, and the rating is some 14% lower. With the more convenient and more economical multi-way ducts, the space requirements are very much less but, owing to closer grouping, the rating is reduced further.

For main runs, possibly the only advantage of ducting is that provision can be made for future cables without further excavation work. There are, however, many disadvantages; the three most important are: (*a*) Cables which overheat, due to overload or to a fault, become so firmly attached to the ducting that they cannot be withdrawn; (*b*) the excessive cost and difficulty of siting suitably constructed manholes at all main junctions and cable-jointing stations (with ducting, joints can be made only at common points); and (*c*) unless encased in concrete, the route of ducting must be kept free of heavy loads.

Cables Cleated or Suspended from Hangers in Trenches or Culverts—There can be little doubt that steelworks electrical engineers would prefer to have all cables, and trunk cables in particular, racked in commodious cable culverts, and if this is impractical or too costly, to have them racked in properly designed trenches with removable covers along the route. Such a layout at once gives the entire electical installation a high degree of reliability and avail-

Table II

HIGH-TENSION CABLE RATINGS UP TO 11 kV.

No. of Cables	Arrangement: Grouping	Arrangement: Spacing, in.	Excavation: Width, in.	Excavation: Depth, in.	Rating Factor *
3-core, P.I.L.C.S.W.A. cables, buried direct in ground; thermal resistivity, G = 120					
4		12	36	54	0·64
6		12	48	54	0·575
9		12	48	72	0·51
12		12	48	90	0·46
4		-	24	42	0·625
6		-	24	42	0·55
12		-	24	42	0·41
Cables run in 3, 6 and 9-way ducts, buried in ground					
3		-	24	36	0·65
6		-	24	45	0·535
9		-	24	54	0·445

*** The rating of the cables for the specified conditions is given as a factor of the capacity of a single 3-core P.I.L.C.S.W.A. cable buried direct.**

ability, and the cable-distributing network has all those facilities and advantages regarded hitherto as the normal requirements for machines, transformers, switchgear, and control gear.

The constructional advantage of cable culverts over all other methods of running cables underground, is that the culverts can be routed under main roads and rail roads carrying heavy traffic and can be incorporated in the foundation of buildings, even if the floors have to support heavy loads. The cables are readily accessible for inspection and, as in the case of machines, etc., if there is occasion to overload feeders, they can be kept under observation. In the event of failure, replacement or repair is straightforward. Additional cables can be installed with the minimum of interruption or inconvenience.

The fixing of cable sizes, from the point of view of heating, owing to load cycles and diversity, is complex. The buried-direct or duct system is inflexible and does not permit of any readjustment after installation, nor is it easy to carry out tests to ascertain the heating of individual feeders ; on the other hand, with the culvert or trench system, tests can be carried out easily, and natural or forced ventilation can be introduced to correct any general overheating due to insufficient space or to added load.

At Consett trunk feeders have been installed for the greater part of the length in a system of culverts and trenches. With the layout of the power station and high-tension switch houses, as shown in Fig. 1, it was convenient to have a commodious cable culvert running the whole length of the station. At the south-east it is extended some 200 ft. to a point from which two subsidiary culverts branch away, one approximately 100 ft. to an existing link house for trunk D.C. feeders, and the other approximately 200 ft. to join a double trench running the whole length of the blast-furnace plant. Provision is made at the north-east for a main culvert to run direct into the new mills. Ample accommodation is provided for the main high-tension interconnecting feeders between the switchgear and the power-station equipment, generators, transformers, etc.[12]

Cable Ratings

On main distributing systems, particularly where the power to be handled is considerable, the selection of cable sizes for individual services is governed by factors other than current-carrying capacity. For example, the fault short-circuit current may, at all or most parts of the network, be 20–40 k.amp., and, even with the most modern switchgear and protective devices the whole or most of these currents may be maintained for 0·5–1·5 sec., with a consequent rapid rise of temperature. The predominating factor is therefore the maximum permissible conductor temperature, *i.e.*, the highest current which can be sustained, on occasion, without causing any deterioration during the time period of the short circuit. There will also be excessive expansion of the conductor, and mechanical stresses will be set up by magnetic repulsion.

Thus, as switchgear is selected on a basis of breaking capacity, so cables should be selected on a basis of short-circuit rating.

Short-Circuit Ratings

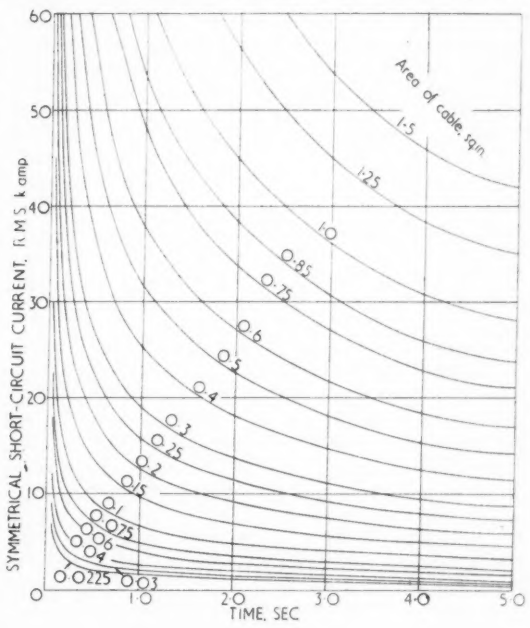
As impregnated-paper insulation carbonizes at approximately 300° C. and as the melting point of solder (used for joints and terminals) is much below this figure, an acceptable maximum temperature for fault conditions has been fixed at around 130° C.

Assuming that the fault current remains constant from the inception to the clearing of the fault and that all heat generated is absorbed in the conductor itself, the cable size for various conditions of current, time, and initial and final temperatures, is determined from the following expression :

$$A = \frac{I\sqrt{t}}{K\left[\log_e \frac{1 + a(\theta + \theta c - 20)}{1 + a(\theta c - 20)}\right]^{\frac{1}{2}}}$$

where A = area of conductor (square inches)
I = short-circuit current
t = time (seconds) of short-circuit period
K = constant
a = temperature coefficient of resistance for copper at 20° C. (0·00393)
θc = conductor temperature (degrees Celsius) at start of short circuit
θ = temperature rise (degrees Celsius)

Thus, with agreed values for initial and maximum conductor temperatures, a series of curves can be drawn to show the relation between cable size and magnitude



NOTES

(1) The curves are based upon
Initial temperature θc = 70° C.
Temperature rise θ = 60° C.
Ultimate temp. $\theta + \theta c$ = 130° C.

(2) The fault current is assumed to be constant and unaffected by the change in conductor resistance incidental to the increased temperature

(3) For values of current, time, and cable size other than those given, a close approximation for the above conditions is given by :

$$\text{Temp. rise (° C./min.)} = \frac{(\text{Current})^2}{(\text{Section, mm.})^2} \times 3$$

Fig. 10—Cable rating for fault currents. Short-circuit/time characteristic curves

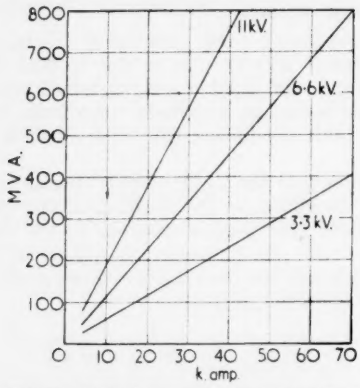


Fig. 11—Curves giving current corresponding to MVA. capacity at 3·3, 6·6, and 11 kV.

and duration of the short-circuit current. Figure 10 gives such a series of curves based on an initial temperature of 70° C. (the maximum permissible normal working temperature under maximum loading conditions) and a maximum temperature of 130° C. From Fig. 11 the values of symmetrical short-circuit currents for various values of MVA. fault capacity at 3·3, 6·6, and 11 kV. may be obtained.

The following figures show the large size of cables required on typical systems at 3·3, 6·6, and 11 kV., and illustrate the economies effected by using modern switchgear with extremely fast protective devices :

System Voltage, kV.	System Fault Capacity, MVA.	Symmetrical Fault Current, k.amp.	Time, sec.	Area of Cable, sq. in.
3·3	100	17·5	0·5	0·15
			1·0	0·20
			1·5	0·30
6·6	250	22·0	0·5	0·20
			1·0	0·25
			1·5	0·40
11·0	500	26·5	0·5	0·25
			1·0	0·30
			1·5	0·40

Taking 200 kVA. at 3·3 kV., 500 kVA. at 6·6 kV., and 2000 kVA. at 11 kV., as representative of the smallest units which would be connected to each system, with corresponding currents of 35, 44, and 105 amp., the smallest size of high-tension cable, *i.e.*, 0·0225 sq. in. for 3·3 and 6·6 kV. and 0·04 sq. in. for 11 kV., would be required on a normal basis.

Such large and costly differences between normal and fault requirements tend to make engineers ignore the desirability of selecting cables in any but the orthodox manner. It is argued that the possibility of a short circuit is so remote that no great risk is taken and that in the event of a complete failure of the cable the only cost to be met is that for a comparatively small cable.

For ordinary undertakings where infrequent but possibly long interruptions of supply may be accepted as standard practice, this may be economical. For steelplants, however, such an argument is fallacious, for, whilst cables may be subjected to faults of varying severity at unknown times without breaking down, the true condition of cables, unlike almost all other types of electrical apparatus, cannot be ascertained by testing or inspection. There is, therefore, always the possibility of operating sections of the plant which are potentially unsound. Since the normal cable requirements for main trunk feeders would conform, more or less, to the fault requirements, such sections would include key units such as pumps for blast-furnaces, mills, gas-cleaning-plant auxiliaries, and power-station auxiliaries—a condition not to be tolerated on steelplants.

On the other hand, the conditions assumed for the selection of cable size under fault conditions (Fig. 10) give results very much on the safe side. A more accurate assessment can be made by taking into account as many as possible of the factors which tend to mitigate the final temperature rise and so give a more economical cable size. The more important of these factors are : (1) Correction for current decrement over the period, and (2) actual or estimated initial temperature.

Correction for Current Decrement—For circuit-breaker selection the only value of reactance applicable is the sub-transient ; this reactance value is taken usually for the cable fault rating. In fact, with modern protective equipment the duration of fault conditions is within the period covered by the transient reactance.

Estimated system-current/decrement curves are given in Fig. 12 ; curve *A* (symmetrical R.M.S. current) shows that the current falls to 0·24 of its initial value after only 1 sec., and the mean R.M.S. (heating) value over this period is only 0·44 of the calculated symmetrical short-circuit value. Thus, a cable selected from the curves in Fig. 10 for a final temperature of 130° C. and an initial conductor temperature of 70° C. would require to be only 0·44 of the size indicated, heating being a function of $I^2R\,A$.

With a 3-phase short circuit, the conductors of one phase will very probably have to carry the full asymmetrical fault current. This should be taken into account ; thus, from Fig. 12, curve *C* (asymmetrical R.M.S. system fault current) the actual values,

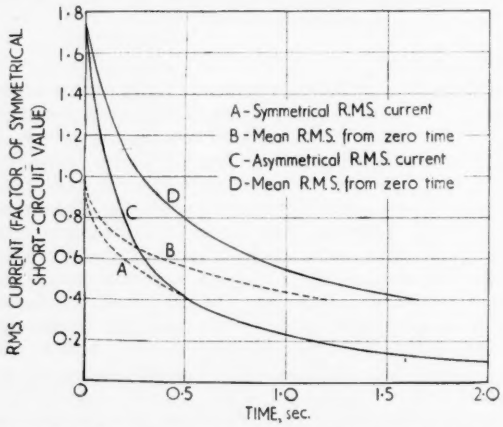
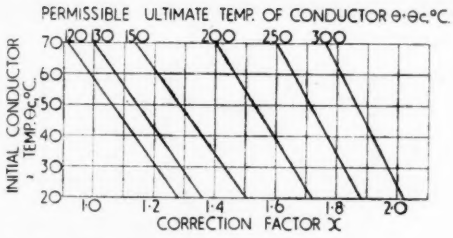


Fig. 12—Cable rating for fault conditions. Estimated system-current/decrement curves for 3-phase short circuit at busbars ; P.F. = 0·02 ; initial conditions, no load. (The values of reactance and resistance for interconnecting cables and stator terminals to switches are assumed to be negligible)



For fixed conditions of :
I and t, area of cable required $= A\,x$
A and I, permissible time $= t \times x^2$
A and t, permissible current $= I \times x$
where I = current (amp.), A = area (sq. in.), t = time (sec.)

Fig. 13—Cable ratings for fault conditions. Corrections for initial conductor temperature θc, with an extended range for final conductor temperature $\theta + \theta c$. (Values taken from Fig. 10, and corrected from Fig. 12)

expressed as a factor of the initial symmetrical R.M.S. current, are :

Current after 1 sec. = 0·24
Mean R.M.S. over the period (curve *D*) = 0·55

Thus, taking the worst case of a feeder with zero resistance, the cable size need be only 55% of the area indicated by the curves in Fig. 10. Taking resistance into account, *i.e.*, a higher power factor, the area would be reduced further.

Actual or Estimated Initial Temperatures—If the maximum continuous temperature of a conductor is 70° C. and the ambient temperature is 20° C., there is a temperature rise of 50° C. For all practical purposes it can be assumed that the temperature rise is a function of I^2 : thus at 50% of cable rating the temperature rise will be :

$$\left(\frac{\text{Actual current}}{\text{Rated current}}\right)^2 \times \frac{50}{1} = \left(\frac{0{\cdot}5}{1}\right)^2 \times \frac{50}{1} = 12{\cdot}5^\circ\text{ C.}$$

and the initial temperature will be $20 + 12{\cdot}5 = 32{\cdot}5^\circ$ C.

From the expression already given for the cross-sectional area of cables, it will be seen that, with a lower initial temperature θc, the cable area will be reduced. Similarly, if the ultimate temperature $(\theta + \theta c)$ is increased, a further reduction in area will be obtained. The curves in Fig. 13 give, for various initial temperatures and an extended range of ultimate temperatures, the correction factor x. The value of x is related to the standard conditions on which the curves are based for Fig. 10, *i.e.*, $\theta c = 70^\circ$ C., $\theta + \theta c = 130^\circ$ C. Thus, for any cable size, for a given symmetrical fault current and time, obtained from Fig. 10 and corrected for mean R.M.S. current, curve *D*, Fig. 12, the final cross-section will be :

$$\text{Area} = \frac{\text{Section of cable (Fig. 10)} \times \text{mean R.M.S. (curve } D\text{) (Fig. 12)}}{\text{Correction factor } x \text{ (initial and final temperature) (Fig. 13)}}$$

The following two examples show the method of calculating the area of cable required.

Example I—Feeder to coke-oven plant at Consett ; 11 kV., 2000 kVA., full-load current 105 amp. ; cable size for normal requirements 0·04 sq. in. ; symmetrical kilo-amperes 26·5 at 500 MVA.

Time	= 1 sec.
Area of cable (Fig. 10)	= 0·42
Mean R.M.S. current (curve *D*, Fig. 12)	= 0·55

This reduces cable to $0{\cdot}42 \times 0{\cdot}55 = 0{\cdot}232$; assume final selection to be 0·15 sq. in.

Full-load current (0·15 cable)	= 250 amp.
Normal full-load current	= 105 amp.
Temperature rise	$= \left(\frac{105}{250}\right)^2 \times 50$
	= 9° C.
With ambient 20° C., θc	= 29° C.
Let $\theta + \theta c$	= 150° C.
From Fig. 13, x	= 1·44
Size of cable required	$= \frac{0{\cdot}42 \times 0{\cdot}55}{1{\cdot}44}$
	= 0·16 sq. in.
Cable actually installed	= 0·15 sq. in.

Example II—Cables for 3·3-kV. auxiliaries at Consett ; 200 kVA., full-load current 34·5 amp. ; cable size for normal requirements 0·0225 sq. in. ; symmetrical kilo-amperes 17·5 at 100 MVA.

Time	= 1 sec.
Area of cable (Fig. 10)	= 0·275
Mean R.M.S. current (curve *D*, Fig. 12)	= 0·55

This reduces cable to $0{\cdot}275 \times 0{\cdot}55 = 0{\cdot}15$ sq. in. ; assume final selection to be 0·10 sq. in.

Full-load current (0·10 cable)	= 210 amp.
Normal full-load current	= 34·5 amp.
Temperature rise	$= \left(\frac{34{\cdot}5}{210}\right)^2 \times 50$
	= 1·3° C.
With ambient 20° C., say, θc	= 25° C.
Let $\theta + \theta c$	= 150° C.
From Fig. 13, x	= 1·47
Size of cable required	$= \frac{0{\cdot}275 \times 0{\cdot}55}{1{\cdot}47}$
	= 0·103 sq. in.
Cable actually installed	= 0·10 sq. in.

Heating of Lead Sheathing

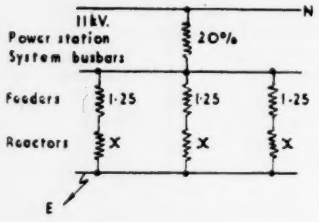
So far, in dealing with the problem of limitations in cable rating under fault conditions, no account has been taken of the temperature of the lead sheath. This can be dealt with in a manner exactly similar to that adopted for the copper conductor.

The current-carrying capacity of the lead sheath for cables of 0·10 sq. in. and larger sizes is much inferior to that of the copper. On systems where the neutral may be connected to earth, direct or through a very low resistance or impedance, and where the earth fault current is equal to, or approaches, the symmetrical R.M.S. fault current, the temperature of the lead sheath would greatly exceed the maximum of 130° C.

At Consett, to give the maximum safeguard to the alternator stator windings, a non-inductive resistance has been introduced between the neutral of one alternator and earth, the resistance being designed to limit the maximum earth-fault current to the full-load value of the machine. Provision is made to earth any one of the alternators through this resistance.

Similar arrangements have been made for the 3·3-kV. windings of the three 5000-kVA., 11/3·3-kV. transformers. This is necessary since the three 3·3-kV. sections of the board are operated as separate units.

Thus, under these conditions and with the lead-sheath current limited to the full-load current of the conductor, the question of sheath temperatures does not arise.[13, 14]



EXAMPLE I

Station : main mill motor house
Load : 10,000 kVA. per feeder
Feeders : Three, each 400 yards, 2 × 0·25 sq. in., 3-core ǁ. Equivalent reactance = 5·5% per mile = 1·25% per 400 yards

Subsidiary switchgear = 250 MVA.
Equivalent system reactance at subsidiary busbars = 40%

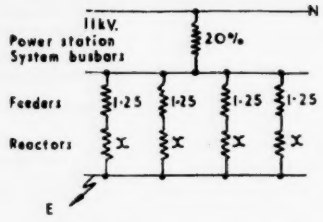
$$20 + \frac{x + 1{\cdot}25}{3} = 40$$

$$x = 58{\cdot}75$$

∴ % Reactance at 10,000 kVA.

$$= \frac{58{\cdot}75}{100} \times \frac{10}{1} = 5{\cdot}875\%$$

Reactor to carry continuously 525 amp.
Reactive drop = 5·875%



EXAMPLE II

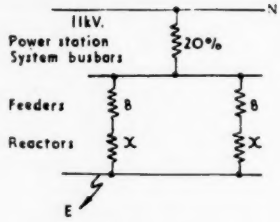
Conditions as for Example I, but with 4 parallel feeders, each 10,000 kVA.

$$20 + \frac{x + 1{\cdot}25}{4} = 40$$

$$x = 78{\cdot}75$$

∴ % Reactance at 10,000 kVA.
= 7·875%

Current rating = 525 amp.



EXAMPLE III

Station : Steelworks sub-station
Load : 2000 kVA. normal, 4000 kVA. max.
Feeders : two, each 1200 yards, 0·15 sq. in., 3-core ǁ. Equivalent reactance = 11·5% per mile = 8% per 1200 yards

Subsidiary switchgear = 150 MVA.
Equivalent system reactance at subsidiary busbars = 66·6%

$$20 + \frac{x + 8}{2} = 66{\cdot}6$$

$$x = 85{\cdot}5$$

∴ % Reactance at 2000 kVA.
= 1·7 %
Current rating = 105 amp.
% Reactance at 4000 kVA.
= 3·4%
Current rating = 210 amp.

Fig. 14—11-kV. feeder reactors. System fault capacity; power-station busbars 500 MVA. (All values of equivalent reactance per mile, on 100-MVA. basis, taken from Fig. 15)

11-kV. Feeders to Mills, Sub-Stations, etc.—Use of Reactors

In view of the very high cost of 500-MVA., 11-kV. circuit breakers, 500-MVA. switchgear should be installed at subsidiary switching stations only where it is not practicable to introduce feeder reactors or to arrange the layout so that all switching is carried out at the central power station.

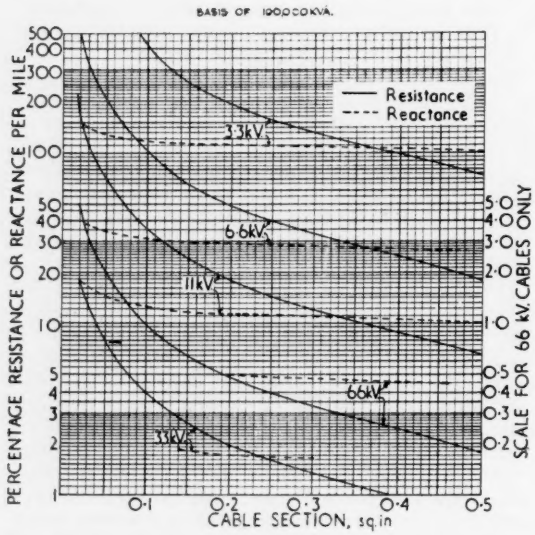


Fig. 15—Curves for resistance and reactance per mile of standard cables, expressed in terms of volt drop as a percentage of phase-to-neutral voltage, to a basis of 100,000 kVA.

To effect any saving with a system fault capacity of 500 MVA., it would be necessary to have subsidiary switchgear of not more than 250 MVA. (*see* Fig. 7).

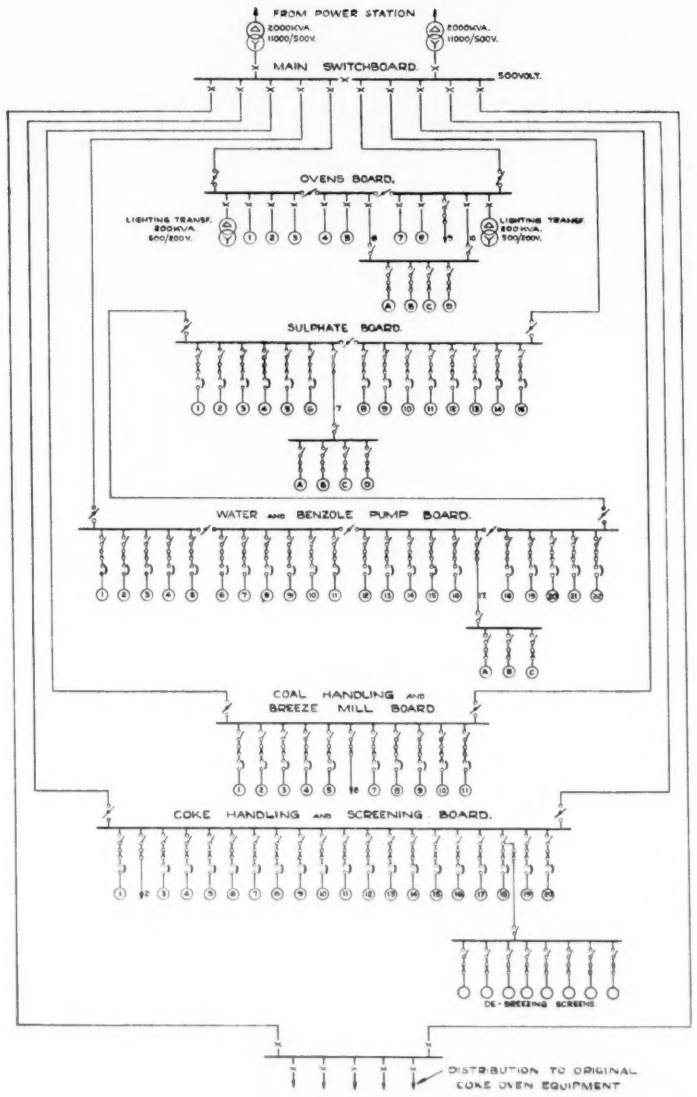
In general, on any section of plant handling a considerable load and requiring more than two feeders, which may be in parallel for normal operations, or even for the short period required to transfer load from one feeder to another, it is neither practical nor economical to introduce feeder reactors.

Calculations for feeder reactors have been worked out for a number of cases (*see* Fig. 14), and to obviate the tedious calculations associated with cable reactance, curves are reproduced in Fig. 15 for percentage reactance and resistance per mile of standard cables for 3·3, 6·6, 11, 33, and 66 kV. to a base of 100 MVA.

From examples I and II (Fig. 14), it will be seen that reactors must be designed, as for switchgear, for the ultimate conditions, and that any reactors introduced when, say, only three feeders are in service must be suitable to limit the MVA. to the value required with additional feeders.

In example II (Fig. 14), the reactance of 7·875% at 10,000 kVA. is excessive, and with a power factor of 0·8 would seriously affect the voltage regulation, particularly since the system regulation will be affected, with tie-bar reactors, if a fourth generator is connected to the bars. Further, in this example, with comparatively high reactance and a continuous current of 525 amp., and the additional cost of 11-kV. joints, etc., it is doubtful whether any appreciable saving could be made.

On the other hand, example III (Fig. 14) shows that, for moderate loads with duplicate feeders, it is possible to install reactors and switchgear of 150 MVA. at approximately 60% of the cost of 500-MVA. gear without seriously affecting the regulation.



SCHEDULE OF EQUIPMENT.

BOARD	REF.	DUTY.			H.P.
MAIN SWITCHBOARD.	1	ORIGINAL COKE OVEN EQUIPMENT BOARD.			
	2	COKE HANDLING & SCREENING BOARD.			
	3	INCOMER Nº1. 2000KVA TRANSFORMER.			
	4	COAL HANDLING & BREEZE MILL BOARD.			
	5	WATER, BENZOLE & SULPHATE BOARD.			
	6	OVENS BOARD.			
	7	OVENS BOARD.			
	8	SULPHATE, WATER & BENZOLE BOARD.			
	9	COAL HANDLING & BREEZE MILL BOARD.			
	10	INCOMER Nº2. 2000KVA TRANSFORMER.			
	11	COKE HANDLING & SCREENING BOARD.			
	12	ORIGINAL COKE OVEN EQUIPMENT BOARD.			
OVENS BOARD	1	REVERSING MACHINE.			10
	2	COAL SPILLAGE CRANE	HOIST.		15
			SLEWING.		3
	3	CLAY MIXER.			20
	4	PUSHER MACHINE	TRAVEL. (4).		15
			RAM.		80
			LEVELLER.		30
			EXTRACTOR.		5
			DOOR HOOK.		2·5
	5	CLAY CARRIER.	TRAVEL.		2·5
			HOIST		2·5
	6	SWITCH & FUSE DISTRIBUTION BOARD.	A	B.F. GAS REGULATOR.	0·75
			B	COLLECTING MAIN REG.	0·75
			C	STACK DRAUGHT REG.	0·75
			D	TICKET PRINTING.	
	7	COAL CHARGING CAR.	SWAB CRANES. (2).		2·5
			TRAVEL.		30
	8	DOOR MACHINE.	EXTRACTOR.		5.
			DOOR HOOK.		2·5
			TRAVEL.		15
	9	SWITCH FUSE.			
	10	ALTERNATIVE SUPPLY TO PANEL 6.			
SULPHATE BOARD.	1	FLUSHING LIQUOR PUMP.			100
	2	AMMONIA " "			6
	3	MOTHER LIQUOR CIRCULATING PUMP.			2·5
	4	AIR COMPRESSOR.			180
	5	CENTRIFUGUL DRIER.			9
	6	" "			9
	7	SWITCH & FUSE DISTRIBUTION BOARD	A	SUCTION MAIN REGULR	0·75
			B	TAR PRECIPITATOR.	10KW
			C	SULPHATE LOADER.	3
			D	WELDING PLUGS.	
	8	CENTRIFUGAL DRIER.			9
	9	" "			9
	10	AIR COMPRESSOR.			180
	11	MOTHER LIQUOR CIRCULATING PUMP.			2·5
	12	SULPHATE BELT Nº1.			3
	13	" " Nº2.			3
	14	SULPHATE DRIER	DRIVE.		5
			FAN.		8
	15	WASTE LIQUOR PUMP.			21

BOARD	REF.	DUTY.			H.P.
WATER & BENZOLE BOARD.	1	FAN Nº1.			16
	2	FAN Nº2.			16
	3	FRAME 'A' PUMP. CIRCULATING.			45
	4	" " " WATER TO APPARATUS.			65
	5	" " " " " "			65
	6	FRAME 'B' PUMP. CIRCULATING.			22·5
	7	WATER TO FINAL COOLER.			32·5
	8	" " " "			32·5
	9	FINAL COOLER. RE-CIRCULATING.			22·5
	10	FAN Nº3.			16
	11	FAN Nº4.			16
	12	LIGHT OIL COLUMN PUMP. REFLUX.			3
	13	OIL PUMP AT FINAL COOLER.			12
	14	HOT WASH OIL PUMP. SCRUBBER.			22·5
	15	" " " "			30
	16	BENZOLISED OIL PUMP.			22·5
	17	SWITCH & FUSE DISTRIBUTION BOARD.	A	WELDING PLUGS.	
			B	NAPTHALENE OIL PUMP	5
			C	NAPTHALENE CRYSTAL[1]	
	18	HOT WASH OIL PUMP.			30
	19	" " " " SCRUBBER.			22·5
	20	OIL PUMP AT FINAL COOLERS.			12.
	21	INTERCEPTING SUMP WATER PUMP.			3
	22	REFLUX PUMP RECTIFYING STILL.			6
COAL HANDLING BOARD.	1	BELT Nº5.			3
	2	BELT 'F'			7·5
	3	ROD MILL.			140
	4	ROD MILL.			140
	5	COAL CRUSHER.			350
	6	SWITCH & FUSE DISTRIBUTION BOARD	BELT 'D' ROTATE.		3
			" 'D' RUN.		3
			" 'E'		5
	7	BELT 'G' BREEZE.			3
	8	SHERWEN VIBRATORY FEEDERS.			
	9	MAGNETIC PULLEY. BELT 'A'.			30
	10	COAL CRUSHER.			350
	11	MAGNETIC PULLEY. BELT 'C'.			50
COKE HANDLING & SCREENING BOARD.	1	QUENCHING PUMP.			50
	2	WELDING PLUGS.			
	3	CONVEYOR BELT Nº1. WHARF.			20
	4	" " Nº2.			8
	5	GYREX SCREEN. DOUBLE DECK.			10
	6	COKE CUTTER.			15
	7	CONVEYOR BELT Nº 14.			5.
	8	" " Nº4.			8
	9	COKE CUTTER.			15
	10	PRIMARY GRADING SCREEN. NORDBERG.			8
	11	SECONDARY " " "			5
	12	CONVEYOR BELT Nº6.			5
	13	" " Nº7.			5
	14	" " Nº11.			5
	15	" " Nº12.			5
	16	" " Nº13.			5
	17	BREEZE CONVEYOR.			3.
	18	ROTARY FEEDER.			3.
		DE-BREEZING SCREENS.			
	19	SHUTTLE CONVEYOR BELT.			3
	20	BREEZE ELEVATOR.			3

Fig 16 Fell coke works. Medium-pressure distribution system

At Consett, air-cored-type reactors are being installed at the power-station end of the steelworks rectifier sub-station feeders ; although the fault current has been reduced considerably, no reduction in cable size is permissible since the ultimate load per feeder may be 4000 kVA.

The characteristics of these reactors will be as follows :

$$\text{Continuous current rating} = \frac{4000 \times 1000}{11{,}000 \times 1{\cdot}73} = 210 \text{ amp.}$$

$$\text{Reactance voltage per phase} = 3{\cdot}4\% \text{ of } \frac{11{,}000}{1{\cdot}73} = 216 \text{ V.}$$

$$\text{Equivalent capacity} = 3{\cdot}4\% \text{ of } 4000 = 136 \text{ kVA.}$$

$$\text{or} = 210 \times 216 \times 3 = 136 \text{ kVA.}$$

$$\text{Reactance per phase} = \frac{216}{210} = 1{\cdot}03 \text{ ohm.}$$

$$\text{Short-circuit current} = \frac{150{,}000 \times 1000}{11{,}000 \times 1{\cdot}73} = 7900 \text{ amp.}$$

$$= 37{\cdot}6 \text{ times F.L. current}$$

It is interesting to note that for a given condition of feeders and fault MVA., the percentage reactance drop is a function of the load.

Remote Sub-Stations Switched from Power Stations

On certain static sub-stations with an arrangement as shown in Fig. 16, for the 2000-kVA. coke-oven plant at Consett, the need for high-tension switchgear at the sub-station can be eliminated. In this case the Solkor system of feeder and transformer protection has been used ; pilot cables interconnect the relays at the 11-kV. switch to the corresponding relays on the medium-pressure circuit breakers controlling the secondary side of the transformers in a sub-station approximately 1200 yards from the power station.

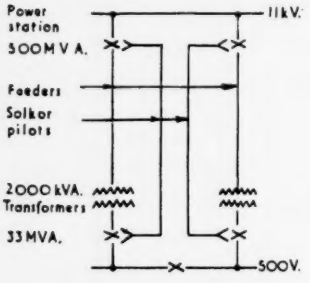
In a coke-oven plant, where it is vital to have 100% availability of supply, the transformers must be rated each for the full plant load.

The saving over the orthodox arrangement of sub-station with high-tension gear, even with switchgear of reduced MVA. capacity, by the introduction of reactors, is considerable and is shown diagrammatically in Fig. 17 to be approximately £4000.

A.C. Medium-Pressure Distribution (500-V., 3-Phase)

The problems associated with medium-pressure distribution are similar to those encountered in high-tension systems, although the fault currents involved are much larger. With transformers of 1500–2500 kVA. the fault capacity of the medium-pressure distribution gear is 25–37·5 MVA., giving fault currents of 29–43·5 k.amp., calculated on the same basis as for the high-tension network.

Whereas, in all previous calculations, resistance has been regarded as having a negligible effect on the fault conditions, it is an important factor at medium pressures. The problem has been dealt with very



Power station : two 500-MVA., 11-kV. oil circuit breakers

Feeders : two 0·15-sq. in., 3-core P.I.L.C.S.W.A. cables

Two 2000-kVA. transformers

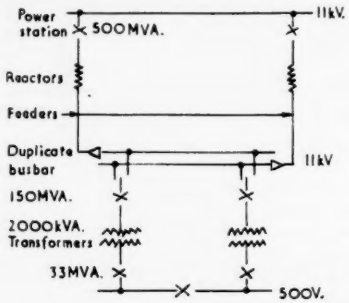
Two 3000-amp., 33-MVA., medium-pressure oil circuit breakers

One 3000-amp., 33-MVA., medium-pressure oil circuit breaker (section switch)

One Solkor pilot cable

Solkor relays

Example I—Solkor feeder and transformer protection. High-tension switchgear at sub-station eliminated



Power station : two 500-MVA., 11-kV. oil circuit breakers

Feeders : two 0·15-in., 3-core, P.I.L.C.S.W.A. cables
Two reactors
Two duplicate busbar 150-MVA. oil circuit breakers

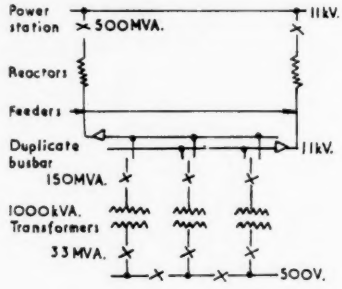
Two 2000-kVA. transformers

Two 3000-amp., 33-MVA., medium-pressure oil circuit breakers

One 3000-amp., 33-MVA., medium-pressure oil circuit breaker (section switch)

Additional building space for high-tension gear
Cost : £3500 more than Example I

Example II — 150-MVA., 11-kV. switchgear at sub-station. Two 2000-kVA. transformers



Power station : two 500-MVA., 11-kV. oil circuit breakers

Feeders : two 0·15-in. 3-core, P.I.L.C.S.W.A. cables
Two reactors
Three duplicate busbar 150-MVA. oil circuit breakers

Three 1000-kVA. transformers

Three 2000-amp., 33-MVA., medium-pressure oil circuit breakers

Two 2000-amp., 33-MVA., medium-pressure oil circuit breakers (section switches)

Additional building space for high-tension gear
Cost : £4500 more than Example I

Example III—As for Example II, but with three 1000-kVA. transformers

Fig. 17—Examples of the layout of a static sub-station

fully, with particular reference to current breaker selection for medium-voltage power circuits, in a paper by Trencham and Morlock.[15] They provided curves which gave a reduction figure for MVA. rating of medium-pressure switchgear, having regard to the impedance of bare copper connections and busbars, and of single and 3-core cables, over a range of 220 to 3·3 kV. The curves can be used equally well for the fixing of cable sizes under fault conditions, and one set of curves is shown in Fig. 18, giving the reduction factor for copper conductors at 440/550/660 V., and for 3-core cables at 440 V.

On most systems where the total connected transformer capacity is of the order of 500 to 1000 kVA., the main distributing switchgear may take the form of air-break switches and H.R.C. fuses. On such boards the question of circuit breaker and cable rating for fault conditions does not arise, apart from such parts as busbars, which may not be protected by the H.R.C. fuses. This is particularly so since H.R.C. fuses up to 50 MVA. at 440 V. are now available. Such factors are important for steelworks plant where transformer capacity may be 1000 to 4000 kVA., outgoing feeders may be required with capacities up to 500 to 800 amp., and for operational purposes it is most desirable to have circuit breakers controlling all feeders.

No hard and fast rules can be given for the layout and installation of a medium-pressure system but, in general, the recommendations given for high-tension systems are applicable.

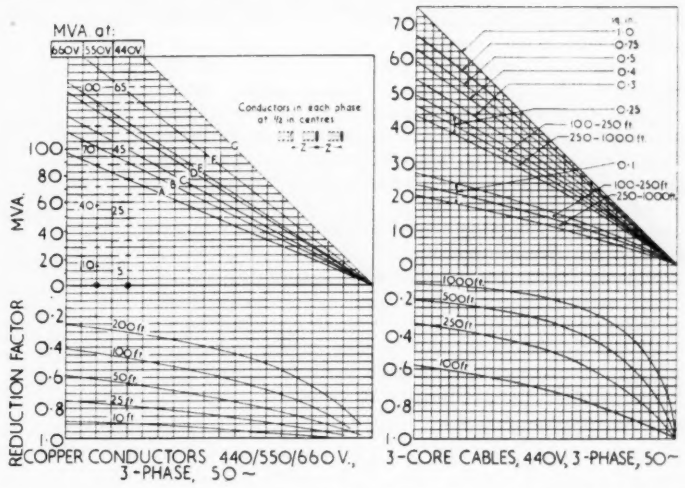


Fig. 18—Selection of switchgear and cables for medium-pressure systems

Medium-Pressure Control Gear

Until 1940, medium-pressure A.C. power was used at Consett, only in the brickworks, coke ovens, and constructional shops, with a total transformer capacity of approximately 3000 kVA. The method of distribution followed the then accepted idea of plant layout, the main distribution switchgear feeding sub-switchgear or distribution boards, from which in turn the individual starting and control units were serviced. These starters and controllers were invariably of the auto-transformer, star delta, or direct-on oil-immersed type, and were placed as near as possible to the individual motors.

Although there are certain advantages in having the starting equipment adjacent to the motors, the present trend is to break away from this system of local distribution boards with starters distributed all over the plant. In an integrated iron and steel works, it is nearly always possible, in a particular section of the plant, *i.e.*, coke ovens, ore handling, or brickworks, for the control equipment for the whole of the motor installation to be grouped together on one or more switchboards and disposed so that the individual motor cables can be distributed neatly and conveniently. It is then possible to provide suitably constructed control buildings, which are very necessary if the control equipment is be to kept in condition and properly maintained. Availability of plant for both operational and maintenance purposes can be obtained, with the old system, only at considerable expense; but with the grouped arrangement it is a simple matter to provide duplicate feeders and any sectionalizing required to permit the continuous operation of the whole or the greater part of the plant, at the same time allowing inspection and maintenance work to be carried out on spare units.

With the 'local starting system' most of the starters, particularly in the early years, were of the oil-immersed type. One of the essentials for any unit of plant is the ability to control the motor at the point of application; for efficient maintenance and repairs to the motor or to the drive, such control is imperative. It is, of course, possible, but not economical, to operate oil-immersed control gear from distant points, and this does not lend itself so readily as does air-break contactor gear to neat arrangement, particularly where it is essential to have operation in sequence. The advantages to be obtained with air-break, multi-unit motor control boards are therefore:

(1) Ease of control, local or remote
(2) Automatic or sequence operation
(3) Accessibility for repairs and maintenance
(4) Grouped starters are housed together away from dirt.

Facilities for repair and maintenance are far superior to those of individual and scattered local starters. The control house can be clean, and spares and replacements can be stocked conveniently in the same building.

Multi-panel contactor boards can be of the open or enclosed types. Open-type boards should be used only when they can be located in air-conditioned or pressurized houses. Enclosed, dust-tight boards can be used in an open building, but a separate house is preferred.

Figure 16 shows the complete medium-pressure installation for the new coke ovens at Fell coke works; all the foregoing recommendations have

been included. Duplicate feeders are used to all outlying and to heavily loaded control boards, and the boards that are reasonably close together and of suitable loading are arranged on a ring main ; thus full provision is made for continuity of supply. The boards themselves, where necessary, have been sectionalized, and the units are grouped for complete accessibility without interrupting the operation of the plant.

Flame-proof starting equipment has not been installed for benzole pumps. A standard control board is used, with flame-proof control points adjacent to the motors in the danger area. The individual starting panels are arranged for complete accessibility to all but the incoming connections to the isolating switches. For main and control circuits, 25-MVA., H.R.C. fuses are fitted throughout.

D.C. Distribution, 500 V.

Until recently the rotary convertor was the accepted medium of conversion from high-tension A.C. to D.C. power. Many plants, like Consett, have a large interconnected D.C. distributing network which, even at 500 V., necessitates a large number of heavy trunk feeders. The mercury-arc rectifier has materially altered this position. With new developments and projects it is usual to have unattended mercury-arc sub-stations sited as near as possible to the centre of gravity of the load, with provision for standby capacity at each sub-station ; each installation is then independent and self-contained. With rectifiers, there are certain advantages which are inherent in their construction and design, *viz.*, availability and protection.

Availability—The multiple-tank or bulb-construction of units from 500 kW. at 250 V., and from 1000 kW. at 500 V., affords greater availability of plant.

Protection—In the past, particularly where, to obtain the maximum benefit from diversity of the D.C. load, the system was operated as one interconnected network, considerable trouble was experienced with failures of D.C. circuit breakers (main and auxiliary). This is understandable since the rate of current rise on a single 1500-kW., 500-V., rotary convertor is of the order 3×10^6 amp./sec. While today all D.C. auxiliary equipment includes H.R.C. fuses, main switchboards of the appropriate rating for the fault capacity are very costly. The rectifier, with the more or less standard arrangement of H.R.C. anode fuses, provides the necessary back-up protection.

For main D.C. distribution, P.I.L.C. and armoured cable is invariably used ; control equipment is becoming standardized in the form of open-type multi-unit contactor boards mounted in separate control houses. For the cable installation connecting the control boards to the motors the following types of cable are available :

V.I.R., in numerous finishes, fire-proof, etc.
P.I.L.C., usually armoured
P.I.A.C., not armoured
Cambric, lead-covered
Mineral insulated
Silicone insulated

In a steelplant, numerous applications are particularly suited to each type, although the extremely arduous conditions make it preferable to use a minimum of V.I.R. cable.

POWER-STATION PLANT

The Power-Station Building

The building is entirely steel-framed and of a steel and brick structure with no bearing walls. Natural lighting is provided by adequate windows ; the turbine room and boiler house have glass roofs, and the control room has a dormer in front of the control board. In the finished building, the architectural lines have been preserved and no steel framework is visible.

The main plant, including the boilers, water-treatment equipment, turbo-alternators and blowers, and rotary convertors, are housed in the main building. Offices, medium-pressure switchgear, both A.C. and D.C., and all transformers (for the station auxiliary lighting and for the 3·3-kV. services in the station and on the plant) are housed in an annexe to the main station building. A separate building running parallel to the station annexe accommodates the 11, 5·5, and 3·3-kV. switchgear and reactors, and the station battery and battery board (*see* Figs. 5 and 8). The electric control room (*see* Fig. 19) for the station is above the main switchgear building and is connected to the station proper by a bridge.

Water-Treatment Plant

The raw water is unfiltered moorland water. The water-treatment plant is situated in the north-east corner of the power station and consists of a contact tank in which coagulants consisting of sodium aluminate and aluminium sulphate are added in proportion to the flow of water. After coagulation the water passes through three pressure filters and three base exchange units. The *p*H of the water is checked before and after treatment and is maintained at 9 by injection of caustic soda in proportion to the flow of water.

After treatment the water passes through a heat exchanger where it recovers heat from the continuous blow-down and passes to the primary surge tanks. From there it is pumped by booster pumps through low-pressure heaters, with the condensate from the blowers, to the de-aerators situated on top of the secondary surge tanks. In the de-aerators, which handle all make-up to the boilers, the water is cascaded down in opposition to a flow of steam at 221° F.

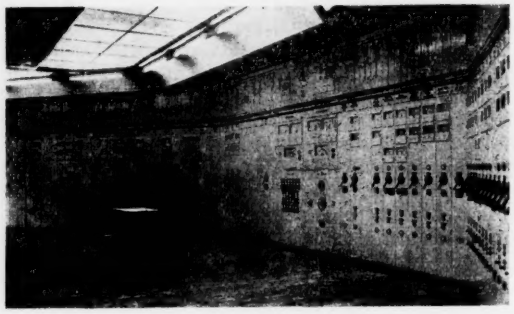


Fig. 19—Control room. Front view of main control board

into the secondary surge tanks. It is then pumped through high-pressure heaters into the boilers by turbine-driven feed pumps. The water is heated to 285° F.

Boiler Plant

The boiler plant comprises three 90,000- and two 120,000-lb./hr. boilers at maximum continous rating. The five boilers are ranged in line on the east side of the station, the boiler feed-pump bay containing the surge tanks and the de-aerators being disposed between the pulverized-fuel and Taylor stoker batteries. Air is preheated by Howden Ljungstrom air preheaters. Flue dust is handled by electrostatic precipitators on the pulverized-fuel boilers and by cyclone-type dust extractors on the Taylor stoker boilers.

The steam-driven boiler feed pumps are designed to operate at either 400 or 160 lb./sq.in., exhausting at 40 lb./sq. in., at which pressure the exhaust steam is used for the high-pressure heaters and for export, and a quantity is reduced to 5 lb./sq. in. for the de-aerators. In addition, the de-aerators receive the flush steam from the continuous blow-down and from the high-pressure heater drains.

Turbo-Blowers

The two new turbo-blowers take steam at 400 lb./sq. in. (gauge) at 750° F. The blowers are single-inlet, centrifugal-type, five-stage units, rated at 50,000 cu. ft./min. at 25–30 lb./sq. in. (gauge). Bled steam is taken through the low-pressure heaters, the water temperature at the heater outlet being maintained at 209° F. by automatic regulation of the steam.

The third (reconditioned) turbo-blower operates from the medium-pressure steam system at 160 lb. sq. in. (gauge) and 550° F. The blower is rated at 40,000 cu. ft./min. at 25 lb./sq. in. (gauge). All air inhaled by the blowers is passed through viscous-type air filters ; owing to the relatively low air velocity through these filters the objectionable noise usually associated with blowers is considerably reduced.

Turbo-Alternators

Two 15,000-kW., M.C.R. 11-kV., 3000-r.p.m. turbo-alternators are in commisssion ; the turbines are of the pass-out type, taking steam at 400 lb./sq. in. (gauge) at 750° F., and are designed to handle 80,000 lb./hr. of pass-out steam at 165 lb. sq. in. (gauge) and 585° F. (*see* Fig. 20).

The condensers are designed for the sets to operate with 'straight condensing' practically up to full load. Each turbine has its own low-pressure heater ; the steam bled from the set is controlled at 230° F. at the heater outlet. The cooling of the alternators is carried out on the closed-air system, the air being circulated by two separate motor-driven fans. Air coolers are of the finned tubular pattern.

The excitation system for each unit includes both main- and sub-exciters ; this feature has been included to improve the response of the automatic voltage-control system when dealing with rapid and frequent fluctuations of the alternator voltage incidental to mill loads.

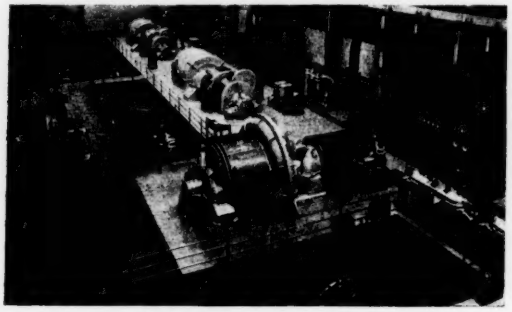


Fig 20—Turbine-room floor

Cooling Tower

Since no natural supply of water is available (the station being approximately 800 ft. above sea level) all condenser cooling water is handled by a hyperbolic-type water-cooling tower, with a reinforced concrete shell, pond, and foundation. The height of the cornice above ground level is 180 ft., and it is 1000 ft. above Ordnance Datum ; the diameter at the base is 128 ft. The normal load is 1,320,000 gal. of water per hour. The tower is divided into two sections as regards distribution, the irrigation and the pond, so that either half can be shut off independently for cleaning purposes.

The make-up water comes from the same source as that for the boiler make-up, but does not pass through the cascading unit ; it is conditioned by adding an alkali, caustic soda, and a tanning phosphate. To prevent alginic growth at the condensers the water is given chlorine treatment.

Rotary Convertors

Three rotary convertors (two already existing, one new), 1500 kW., 500/250 V., D.C., are accommodated on the turbine-room floor. The corresponding transformers which are connected to the 11-kV. system are housed in the annexe in positions convenient to the rotary convertors. The main D.C. switchgear and starters are located close to the convertors and on the engine-room floor.

Auxiliaries

All auxiliaries, other than the 3·3-kV. circulating-water pump motors for the turbo-alternators, are serviced from a main 500-V., metalclad, air-insulated switchboard, fitted with horizontal draw-out-type oil circuit breakers. Maximum availability of supply is ensured by arranging this board in two main sections with a single unit section in the centre : all subsidiary switchboards, etc., are arranged for duplicate or alternate feeders. Multi-motor control boards have been employed wherever possible.

High-Tension Switchgear

The main switch house has separate switch rooms for each section of the 11-kV. and the 3·3-kV. switchboards, and the two bus 11-kV. section switches are in single cells. Fire proof doors, normally closed by gravity, are used. Mono-bias bus-zone protection is fitted to the 11-kV. switchgear.

The switchgear is solenoid-operated ; all control and auxiliary cables are taken to the control room *via* the common cable duct. Between the control-

room floor and the switch-house roof is a space to allow all auxiliary cables to be racked neatly to frames.

Control Board

The control board is of the continuous corridor type, built in three sections. For easy observation from the central desk, the two outer sections are set in at an angle of about 30°.

All indicating instruments, alarms, and switch controls are arranged on the front panel. A line diagram of the switchboard, with automatic indicators, is arranged immediately above the control board (*see* Fig. 19). All relays, including the mono-bias bus-zone equipment, the integrating and recording instruments, and the voltage automatic regulators, are mounted on the back panels. High-tension switching operations are carried out by the control engineer.

Since it is necessary to operate the 3·3-kV. system as three independent sections, all feeder switching operations at all sub-stations and at all main switching points are carried out under the supervision of the control engineer.

Ventilation of the Turbine Room, Control Room, etc.

With modern steelplants, particularly in motor houses containing Ilgner sets, main mill drives for slabbing mills, reversing plate mills, continuous mills, etc., it is accepted practice to install a complete air-conditioning plant so that the building is pressurized to, say, 0·1 to 0·15 in. W.G., to prevent the ingress of dust (usually metal dust).

In general, in the case of power stations, the question of air conditioning and pressurizing does not arise. For a central power unit for an iron and steel plant, however, where it is situated as near as possible to the ultimate centre of gravity of the plant demand, the question of ventilation and air conditioning is just as important as for the mill houses. Metallized dust must be excluded from the turbine room, switch houses, and the control and instrument rooms housing the sensitive relays.

Little or no information is available on the pressurizing of power stations ; for mill work, since the total power losses based on the R.M.S. loading of all machines are available, and with an agreed temperature difference between the inlet and the outlet air, it is not difficult to calculate the quantities of air required. For a power station the problem is more complex ; with a normal layout the air for the boilers, which is handled by the forced-draft fans, is drawn in from all parts of the building, thus creating a negative pressure within the building. With such an arrangement in an iron and steel power plant it would be impossible to prevent the ingress of dust or coke breeze, and it is therefore desirable to have a partition between the boiler house and turbine room.

It is fairly simple to calculate the quantity of air required for the ancillary electrical plant, but it is more difficult to determine the power equivalent of the heat loss by radiation and convection from the turbine casings, the steam mains and valves, and from the turbo-blower casings. By careful approximation it was decided that 160,000 cu. ft. of air per minute would cater for the losses of all the machines which may be installed.

The station is designed for this quantity of air to be handled by two separate air-cleaning stations at the north and south ends of the power station, these cleaners being of the viscous type. Having established the quantity of air and a suitable pressure, in this case 0·15 in. W.G., the outlets for the air must be arranged so as to maintain this pressure. This is done by making the outlet velocity head equal to the desired building pressure. A velocity head of 0·15 in. W.G. is equal to 1640 ft./min. ; thus the maximum area of outlet is 160,000/1640, say, 100 sq. ft.

In a mill building this comparatively large quantity of air could be handled most easily by suitably placed outlets in the roof. In the power station the various outlets will be by the windows in the offices, the cable ducts, pipe trenches, etc., the pressure being controlled by adjustable outlets in the turbine-house roof.

CONCLUSION

It has been possible to cover only certain aspects of the problem of power generation and distribution in iron and steelworks : design and layout, rather than operational problems, have been discussed. Space does not permit discussion of matters such as system earthing, vector grouping of transformers, migration of sealing compound from cables, boxes, and joints, and the selection of cable sizes for squirrel-cage motors, having regard to minimum permissible starting torque with direct-on starting.

Acknowledgment

The author wishes to thank the Consett Iron Co., Ltd., and Mr. H. Boot, Director and General Manager, for the facilities given for preparing this paper.

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N E W S

ANNOUNCEMENTS AND NEWS OF SCIENCE AND INDUSTRY

THE IRON AND STEEL INSTITUTE

Meetings

Annual General Meeting, 1950

The Annual General Meeting of The Iron and Steel Institute will be held in London on Wednesday and Thursday, 26th and 27th April, 1950. After the official business and presentation of awards on the first day, the President-Elect, Mr. J. R. MENZIES-WILSON, O.B.E., will be inducted and will then deliver his Presidential Address. The programme of technical sessions is now being arranged, and full details will be given in the April issue of the *Journal*.

A dinner for Members and their guests will be held at the Dorchester Hotel, on the evening of the second day (Thursday).

Sidney Gilchrist Thomas Centenary

On the evening of the first day (Wednesday) of the Annual General Meeting, Mr. James Mitchell, C.B.E. (Honorary Treasurer) will give a Special Commemorative Lecture, to mark the centenary of the birth of Sidney Gilchrist Thomas on 16th April, 1850.

Special Meeting in Glasgow, 1950

The Council have accepted an invitation of the West of Scotland Iron and Steel Institute to hold a Special Meeting in Glasgow from Tuesday 12th to Friday 15th September, 1950.

The programme will provide for discussions of technical papers and also visits to iron and steel works and engineering concerns in Scotland. In addition to various social activities there will be special excursions and visits for Ladies.

More details of the meeting will be given in future issues of the *Journal*.

Symposium on High-Temperature Steels and Alloys for Gas Turbines

A Symposium will be held on Thursday and Friday, 19th and 20th October, 1950, on the subject of steels and alloys for use under high-temperature conditions, such as occur in the gas turbine.

On the Wednesday evening prior to the Symposium the Fifth Hatfield Memorial Lecture will be delivered by Sir Frank Whittle, K.B.E., C.B., D.Sc., F.R.S.

Autumn General Meeting, 1950

The Autumn General Meeting will be held in London, on Wednesday and Thursday, 15th and 16th November, 1950.

Relations with Foreign Societies

It was announced in 1949 (May issue, p. 104) that arrangements had been made for collaboration of the Institute with the Associazione Italiana di Metallurgia, Milan. It has now been arranged that Members of the Institute may become Members of the Association at the reduced rate of lire 4000 instead of lire 5000.

It should be noted that the Journal " La Metallurgia Italiana " will now be issued monthly. The bulletin " Atti-Notizie " is issued free.

Andrew Carnegie Research Fund

Research Grants and Travelling Scholarships

Andrew Carnegie Research Grants and Travelling Scholarships are awarded to suitable applicants to enable them to conduct researches into the metallurgy of iron and steel. The amount of any one research grant will not normally exceed £250 in one year and the range is usually between £50 and £200. The value of the travelling scholarships will depend upon the circumstances of each case, but will normally be of the order of £500, although smaller or substantially greater sums may be given. The scholarships will usually be tenable for one year and are intended to assist recipients to conduct researches within the Sterling Area.

Conditions

A conditions of the awards is that candidates must be under 35 years of age. There is no restriction on sex or nationality. The results of the research are to be communicated to The Iron and Steel Institute in the form of a report for publication. An Andrew Carnegie Gold or Silver Medal may be awarded to the author of the report which, in the opinion of the Council, is the most meritorious of the year.

Grants are normally paid in four quarterly instalments ; the first on award, the second and third on receipt of satisfactory brief progress reports, and the final instalment on receipt of the completed report.

Application forms may be obtained from the Secretary.

Binding Cases and Holders for the Monthly Journal

After the issue of binding cases for vol. 163 (1949), binding cases will no longer be supplied free but will be available at an annual charge of 10*s*. 6*d*. per set of three, to cover the twelve monthly issues of the *Journal*. This charge will include indexes, packing, and postage.

Arrangements have been made for binding Members' *Journals* at a charge of 8*s*. 6*d*. per volume ; this charge includes packing and postage to Members' addresses, but excludes the cost of the binding case.

Stout holders have been designed into which up to four issues of the monthly *Journal* and one index may be inserted and kept for convenient handling ; single issues of the monthly *Journal* can be inserted or removed from the holders without difficulty. These holders are available on request at a charge of 7*s*. 6*d*. each, including packing and postage.

HISTORICAL RESEARCH

History of the Iron and Steel Industry

Dr. H. R. Schubert, Historical Investigator to The Iron and Steel Institute, and author of the Historical Notes which appear from time to time in the *Journal*, is now preparing a " History of the British Iron and Steel Industry." He is concentrating, in the first instance, on the development of the industry in medieval times up to the period when coke was first employed for smelting. Relatively little is known of the industry during this period. Much research is involved, and readers of the Historical Notes will have realized that Dr. Schubert is unearthing many facts which have hitherto been unknown.

Dr. Schubert, who is a naturalized British subject, is the author of several important books, including the " Geschichte der Nassauischen Eisenindustrie " (jointly with L. Beck), and of the " History of the Buderus Ironworks " up to the beginning of the 19th century—both classics in their fields.

Although it will be some time before the new book is ready, the publication of this authoritative history of the industry in this country will be welcomed by all who are interested in the early development of the manufacture of iron and steel.

The Mushet Family

Mr. Fred M. Osborn, Chairman of Samuel Osborn and Co. Ltd., Clyde Steelworks, Sheffield, is engaged on the writing of a history of the distinguished metallurgists David Mushet (1772–1847) and Robert F. Mushet (1811–1891). David discovered the Blackband ironstone of Scotland, whilst Robert, among other things, was associated with Sir Henry Bessemer in perfecting his process, and also discovered self- or air-hardening steel, the pioneer tool steel.

Mr. Osborn would be grateful if any reader who has recollections of the Mushet era (from about 1860–1900), or who has documents covering this period and bearing on the subject, would write to him. Any information about David Mushet's antecedents (he was born at Dalkeith in Scotland, his father being William Mushet who married Margaret Cochrane) would be appreciated.

NEWS OF MEMBERS

▶ Mr. P. T. ARTHUR, now on the staff of Messrs. Hadfields, Ltd., Sheffield, has been awarded the degree of Ph.D. of the University of Glasgow.

▶ Mr. V. AYTEKIN has been awarded the degree of Ph.D. (Metallurgy) of the University of Birmingham. He left for Turkey on 12th January, 1950.

▶ Dr. S. BHATTACHARYA has been awarded the degree of Ph.D. of Manchester University, and the Cort Silver Medal for research. He has left for India.

▶ Mr. S. BINNER is now on the headquarters staff of the Training Department of Messrs. Stewarts and Lloyds, Ltd., Corby, Northants.

▶ Mr. N. U. BREMNER has left Messrs. Guest, Keen and Baldwins Iron and Steel Co., Ltd., to take up an appointment with Messrs. Richard Thomas and Baldwins, Ltd.

▶ Mr. L. W. DERRY has been appointed Head of the Metallurgy Department of Battersea Polytechnic, London, on the retirement of Dr. W. A. Naish.

▶ Mr. R. DUTT has been awarded the degree of B.Sc. (Eng.) (Met.) of London University. He has left Messrs. Stewarts and Lloyds, Ltd., Corby, and has returned to India.

▶ Mr. D. W. HAMMOND has been appointed Works Director of Messrs. William Jessop and Sons, Ltd., Sheffield.

▶ Mr. D. E. R. HUGHES, of Tube Investments, Ltd., has been awarded the degree of Ph.D. (Metallurgy) of the University of Birmingham.

▶ Mr. J. F. B. JACKSON, Chief Metallurgist and Technical Controller (Foundries) to Messrs. David Brown and Sons, (Huddersfield), Ltd., has been appointed Director of Research to the recently formed Research and Development Division of the British Steel Founders' Association.

One of the main tasks of the Division is to bridge the gap between scientific knowledge and steel-foundry practice for the betterment of production technique, the improvement of the quality of the product, and the reduction of manufacturing costs ; to the direction of this work, Mr. Jackson brings a wide knowledge and background of all branches of steel-foundry technique and management, with which he has combined experience of the commercial application of research and development work.

▶ Mr. J. LEJA has been appointed Metallurgist-Reduction Officer, of the South West Africa Co., Ltd., London.

▶ Mr. W. H. LEWIS, Arc-Furnace Superintendent at the Panteg Works of Messrs. Richard Thomas and Baldwins, Ltd., has been appointed Steelworks Manager.

▶ Mr. T. G. MACFARLANE has resigned his position as metallurgist at Messrs. Stewarts and Lloyds, Ltd. He has been appointed Ferguson Fellow in Applied Chemistry at the Royal Technical College, Glasgow.

▶ Mr. E. MARKS, Honorary Member of Council, and President of the Staffordshire Iron and Steel Institute, has been awarded the degree of Ph.D.

▶ Mr. E. MILLINGTON, Steelworks Manager at the Panteg Works of Messrs. Richard Thomas and Baldwins, Ltd., has been appointed Assistant General Manager.

▶ Mr. J. F. MILLS has joined the Engineering Department of the Metals Division of Imperial Chemical Industries, Ltd., at Witton.

▶ Mr. S. E. ROGERS joined The Steel Company of Wales, Ltd., as a trainee metallurgist, in October, 1949.

▶ Mr. ASHLEY S. WARD has been appointed President of Messrs. Thos. W. Ward, Ltd., Sheffield, on his resignation of the Chairmanship. Mr. G. WOOD has been appointed Chairman, Mr. F. R. STAGG, Deputy Chairman, and Mr. H. W. SECKER, O.B.E. and Mr. J. WALTON, Joint Managing Directors. Mr. F. W. ROBINSON, a local Director, has been appointed to the full board of the Company, to take charge of the London area interests in place of Mr. A. CARR, who has transferred his activities to Sheffield.

▶ Mr. T. H. WILSON has been appointed Staff Foreman of the Arc Furnace Melting Department, Ford Motor Co., Ltd., Dagenham, Essex.

Obituary

Mr. C. HUGO CARLSSON, of Storängen, Sweden, on 22nd December, 1949.

Mr. A. H. NILSSON, of C. O. Öberg and Co.'s Aktiebolag, Eskilstuna, Sweden, in October, 1949.

Dr. F. W. MEYER, of Sulzer Bros., Winterthur, Switzerland.

Ing. VLADIMIR STIEBER, former Director of the Skoda Works, Pilsen, Czechoslovakia, on 6th November, 1949.

CONTRIBUTORS TO THE JOURNAL

A. E. DeBarr, B.Sc., F.Inst.P.—Head of the Physics section of the G.K.N. Research Laboratories. Mr. DeBarr was born in 1918, and was educated at West Leeds High School, Leeds, and at Leeds University, where he gained the degree of B.Sc., with first-class special honours in physics, and did post-graduate research until January 1940. From then until October 1945 he was Temporary Experimental Officer in the Mine Design Department, H.M.S. *Vernon*, engaged in research and development

A. E. DeBarr

T. Coxon

B. Roberts

work on magnetic and electrical circuits. In October 1945 he became Research Superintendent (Physics) in the Research Laboratories of Messrs. Joseph Sankey and Sons, Ltd. (later the G.K.N. Research Laboratories), where he is engaged in research on ferromagnetism, plastic deformation of metals, and recrystallization phenomena.*

T. Coxon—Consulting Electrical Engineer to the Consett Iron Co., Ltd., Co. Durham. Mr. Coxon was educated at Rutherford College, and at the Rutherford Technical College, Newcastle, and served his apprenticeship with Messrs. J. H. Holmes and Co., Ltd., Newcastle. He joined the English Electric Co., Ltd., at their Dick Kerr Works, Preston, in 1922, as Junior Technical Engineer, and worked on traction development testing. In 1923 he took charge of the turbo-alternator test department, and in 1926 he transferred to the test department at Stafford, as Assistant Chief Engineer. From 1927 to 1930 he was Design Engineer, being responsible for rolling mill and colliery winder equipments. In 1930 he joined the Consett Iron Co., Ltd., as Assistant Electrical Engineer of iron and steel works, and took up his present appointment in 1940. Mr. Coxon is a member of the Electrical Engineering Committee of the British Iron and Steel Research Association.

A. S. Kenneford, M.Sc., F.I.M.—Principal Scientific Officer in the Ferrous Section of the Armament Research Establishment, Ministry of Supply. Mr. Kenneford was educated at Queen Elizabeth's Grammar School, Blackburn, Lancashire, and at Manchester University where he was awarded an honours degree in Metallurgy in 1931. He joined the Armament Research Establishment in 1935.

B. Roberts, B.Sc.—On the staff of the G.K.N. Research Laboratories. Mr. Roberts was born in 1927, and was educated at Queen Elizabeth's Grammar School, Mansfield, and at University College, Nottingham, where he obtained the B.Sc.Lond.(Ext.) degree in physics, in 1948. He joined the staff of G.K.N. Research Laboratories in 1948.

J. Taylor, M.Sc., Ph.D.—Senior Lecturer in the Metallurgy Department of the Royal Technical College, Glasgow. Dr. Taylor graduated with first-class honours in chemistry at the former Armstrong College of Durham University in 1926. He then carried out post-graduate research in fuel technology at Imperial College, London, under the late Professor Bone, and in physical chemistry at the Technische Hochscule, Danzig, under Professor von Wartenberg. After some years in the Coal Survey Laboratory, and in the coke-oven industry, he joined the former research section of the British Iron and Steel Federation as Field Tests Officer, on blast-furnace research. During the war he was seconded to the Ministry of Supply, and carried out work on carburization from the point of view of armour plate. He joined the Royal Technical College, Glasgow, under Professor Hay, in 1945.

R. F. Jennings, B.Sc.—Technical Secretary of the Plant Engineering Division of The British Iron and Steel Research Association. Mr. Jennings was born in 1915, and was educated at Perse School, Cambridge, and at Kings College, Newcastle-on-Tyne, where he obtained the degree of B.Sc. in Naval Architecture, in 1937. After apprentice experience in marine engineering on the N.E. coast he joined the then newly formed British Coal Utilisation Research Association, as a Research Officer, in 1938. During the recent war, he was engaged in the development of a number of coal-burning appliances including small mobile gas producers and automatic stokers for burning low-grade fuels. He also worked on the development of special carbon refractories from coal.

He took up his present position in 1946.

G. E. Eden and **G. A. Truesdale.**—Biographical notes were received too late for inclusion. They will appear in the April issue of the *Journal*.

* Mr. DeBarr has since joined the Laboratories of Elliott Bros. (London), Ltd., as Leader of the Physics Division.

J. Taylor

R. F. Jennings

IRON AND STEEL ENGINEERS GROUP

The TWELFTH MEETING of the Iron and Steel Engineers Group will be held at 4 Grosvenor Gardens, London, S.W.1, on Thursday, 23rd March, 1950.

A Buffet Luncheon will be held in the Library in connection with the Meeting.

The following papers will be discussed :

"*Traffic of Iron and Steel Works—a Method of Traffic Analysis*," by H. H. Mardon and M. D. Brisby.

"*Structural Frames for Melting Shops*," by J. S. Terrington.

"*A Proposed Method of Specifying Travel Motion Performance for Overhead Cranes*," by D. A. Wise and L. N. Bramley.

Joint Meeting in Scotland

A Joint Meeting of the Iron and Steel Engineers Group and the West of Scotland Iron and Steel Institute will be held on 2nd March, 1950, at 39 Elmbank Crescent, Glasgow, at 6.45 p.m. Mr. A. I. Nussbaum, a Mond Nickel Fellow for 1948, will give a Lecture entitled "*A Review of Recent American Rolling Mill Installations*."

Joint Meeting at Sheffield

A Joint Meeting of the Iron and Steel Engineers Group and the Sheffield Society of Engineers and Metallurgists will be held on 20th March, 1950, at the Royal Victoria Station Hotel, Sheffield, at 6.15 p.m. Mr. A. I. Nussbaum, a Mond Nickel Fellow for 1948, will give a Lecture entitled "*A Review of Recent American Rolling Mill Installations*."

AFFILIATED LOCAL SOCIETIES

Liverpool Metallurgical Society

The list of Officers for the Session 1949–50 is as follows :

President

S. J. KENNETT, Ph.D.

Vice-President

H. EDWARDS

Hon. Treasurer

H. J. MILLER, M.Sc.

Hon. Secretary

J. A. PARDOE, M.Eng.

Members of Committee

R. S. BROWN, M.B.E.
A. E. GRIFFIN
V. L. FARTHING
J. J. MILES
E. J. STEPHENS, M.Eng.

Sheffield Metallurgical Association

The list of Officers for the Session 1949–50 is as follows :

President

J. WHITE, D.Sc.

Vice-Presidents

E. T. GILL
H. HICKS
E. W. COLBECK
T. R. WALKER

Hon. Treasurer

T. H. ARNOLD

Hon. Secretary

J. BURNETT

Members of Council

H. ALLSOP
J. RAIT, D.Sc.
B. BAGSHAWE
J. E. RUSSELL
S. HARRISON
J. H. SPALTON
W. C. HESELWOOD
J. W. S. STAFFORD
P. JUBB
J. L. WEST

BRITISH IRON AND STEEL RESEARCH ASSOCIATION

Staff

Dr. J. PEARSON has been appointed Head of the Chemistry Department at the British Iron and Steel Research Association in succession to Dr. F. D. Richardson. Dr. Pearson has been Head of the Chemistry Section of the B.I.S.R.A. Laboratories at Sketty Hall, Swansea, since 1946, where he has worked on the disposal of spent pickle liquor, the gas pickling of steel, and the preparation of replicas for surface-finish assessment.

Dr. Pearson is a graduate of London University, and has previously worked with Tube Investments, Ltd., and with the Armament Research Department of the Ministry of Supply, at Woolwich. He received the degree of Ph.D. in 1946 for research on the polarography of nitro-compounds.

In his new post he will be responsible for research on physical chemistry of iron and steel making, refractories, and problems of corrosion.

Mr. G. WITHERS relinquished his position as Technical Secretary to the Corrosion Section of the Metallurgy (General) Division on 31st December, 1949. He has joined the Technical Secretariat of the Federation of British Industries.

INSTITUTE OF METALS

The 42nd Annual General Meeting of The Institute will take place in the Hall of The Institution of Mechanical Engineers, Storey's Gate, London, S.W.1., on Wednesday, 29th March, and Thursday, 30th March, 1950.

On the first morning the new President of the Institute, Mr. H. S. Tasker, Chairman of Goodlass Wall and Lead Industries, Ltd., will be inducted into the Chair, and will deliver his Presidential Address.

During the course of the meeting the Institute of Metals Medal in Platinum will be presented to Professor Albert Portevin, who has expressed his intention of being present.

NEWS OF SCIENCE AND INDUSTRY

Mond Nickel Fellowships

The Mond Nickel Fellowships Committee has pleasure in announcing a further award for 1949 :

Mr. E. BRUNSKILL (Mufulira Copper Mines, Ltd.) : To study metallurgical practice in the mining, smelting, and refining of non-ferrous metals in Africa, with special reference to copper and related metals.

The Mond Nickel Fellowships Committee invites applications for the award of Mond Nickel Fellowships for the year 1950. Awards will be made to selected applicants of British nationality educated to University degree or similar standard, though not necessarily qualified in metallurgy, who wish to undergo a programme of training in industrial establishments ; they will normally take the form of travelling Fellowships—awards for training at Universities may be made in special circumstances. There are no age limits although awards will seldom be given to persons over 35 years of age. Each Fellowship will occupy one full working year. The Committee hope to award up to five Fellowships each year of an average value of £750 each.

Mond Nickel Fellowships will be awarded in furtherance of the following objects :

(*a*) To allow selected persons to pursue such training as will make them better capable of applying the results of research to the problems and processes of the British metallurgical and metal-using industries.

(*b*) To increase the number of persons who, if they are subsequently employed in executive and administrative positions in the British metallurgical and metal-using industries, will be competent to appreciate the technological significance of research and its results.

(*c*) To assist persons with qualifications in metallurgy to obtain additional training helpful in enabling them ultimately to assume executive and administrative positions in British metallurgical and metal-using industries.

(*d*) To provide training facilities whereby persons qualified in Sciences other than Metallurgy may be attracted into the metallurgical field and may help to alleviate the shortage of qualified metallurgists available to industry.

Applicants will be required to state the programme of training in respect of which they are applying for an award, as well as particulars of their education, qualifications, and previous careers. Full particulars and forms of application may be obtained from The Secretary, The Mond Nickel Fellowships Committee, 4 Grosvenor Gardens, London, S.W.1. Completed application forms should reach the Secretary of the Committee not later than 1st June, 1950.

International Congress on Analytical Chemistry

At a meeting held in Amsterdam in September, 1949, the International Union of Pure and Applied Chemistry gave its patronage to an International Congress on Analytical Chemistry, which will be held in Britain in 1952. An Executive Committee, under the Chairmanship of Mr. G. Taylor, President of the Society of Public Analysts, is now making preliminary arrangements in regard to location of the Congress and publication of papers.

The Honorary Secretary of the Congress is Mr. R. C. Chirnside, Research Laboratories, G.E.C. Ltd., Wembley.

Canadian International Trade Fair, 1950

The British tool, machine tool, and scientific instrument manufacturers are combining their resources to prepare a collective display of their goods at the 1950 Canadian International Trade Fair, which is to be held in Toronto. This will be the most determined effort yet made by any group of British industries to secure increased dollar trade.

Festival of Britain 1951

At a Science Conference, held on 26th January in connection with the Festival of Britain 1951, Sir Edward Appleton, F.R.S., outlined to representatives of the principal scientific institutions and the technical press the part that science and technology will play in the Festival. After giving details of the plans for exhibitions on the South Bank of the Thames, at South Kensington, and in the Kelvin Hall, Glasgow, Sir Edward recommended that the Learned Societies and Scientific Institutions should make an important contribution to the interest of the Festival by arranging that 1951 should be an occasion for holding special meetings and conferences. The Council of the British Association, and the Institutions of Civil, Mechanical, and Electrical Engineers have already intimated that they are making special arrangements for meetings to coincide with the Festival.

International Union of Crystallography

The Executive Committee has accepted a kind invitation from the Swedish National Committee of Crystallography to hold the Second General Assembly and International Congress in Stockholm from 27th June to 3rd July, 1951. These dates have been chosen in consultation with the Swedish National Committee and with the National Committee of all the Adhering Bodies. It is hoped that this early notice will make it possible for crystallographers throughout the world to arrange to attend.

Further particulars of the meeting will be given later.

Ironfounding Productivity

With Marshall Aid assistance, the Anglo-American Council on Productivity has arranged for a Productivity Team from the general ironfounding industry to tour the U.S.A. for six weeks. Although the team hopes to inspect both large and small foundries it expects to gain the most useful information on the use of technical knowledge, equipment, and man-power, from the smaller foundries.

The leader of the team is Mr. S. H. Russell, Chairman and Director of S. Russell and Sons, Ltd., Leicester, and the Secretary is Mr. G. B. Judd of Messrs. Mann, Judd and Co., London.

Steel Production in Europe

In the study of "European Steel Trends in the Setting of the World Market," which has been prepared by the Secretariat of the Steel Division of the U.N. Economic Commission for Europe, it is pointed out that overall steel production in 1953 will exceed all estimated demands, including exports, by 8,000,000 tons. The report indicates a number of ways in which greater co-ordination of the steel-producing countries would help to ameliorate this position, and it emphasizes that a reduction of prices to encourage home consumption will be necessary.

Monsieur Rene Nihoul

Monsieur René Nihoul, director of the Belgium-Luxembourg Centre of Information on Steel, and adviser to "Fabrimétal," the Federation of the Industry of Metal Fabrications, died on 18th December, 1949, as a result of an air accident. He had been called to Paris as adviser to the European Organization of Economic Co-operation.

Rockwell Machine Tool Co., Ltd.

The Cincinnati-Planer Co., of Cincinnati, Ohio, U.S.A., has appointed the Rockwell Machine Tool Co., Ltd., as sole selling agent in Great Britain.

This agency, which was in the hands of the Selson Machine Tool Co., has been transferred, as the Cincinnati-Planer Co. is now a wholly owned subsidiary of the Giddings and Lewis Machine Tool Co., for whom Rockwell Machine Tool Co., Ltd., have been sole agents for a number of years.

Head, Wrightson and Co., Ltd.

Mr. Richard Miles, Managing Director, has been elected Chairman of the Company in succession to the late Sir Guy Wrightson, Bt. Sir John Wrightson, Bt., has been appointed Vice-Chairman.

Changes of Address

THE BRITISH OXYGEN CO. LTD.—The head office of this Company is now at Bridgewater House, Cleveland Row, St. James's, London, S.W.1.

THE ASSOCIATION OF HEATING, VENTILATING, AND DOMESTIC ENGINEERING EMPLOYERS has moved to 58 Victoria Street, Westminster, S.W.1.

DIARY

1st Mar.—INSTITUTION OF ENGINEERING INSPECTION (Birmingham Branch)—"*A Logical Approach to the Use of Tool Steels*," by T. W. Whiting—The Chamber of Commerce, 95 New Street, Birmingham, 6.0 P.M.

1st Mar.—MANCHESTER METALLURGICAL SOCIETY—"*Continuous Casting of Metals*," by J. W. Cuthbertson—The Engineers' Club, Albert Square, Manchester, 6.30 P.M.

2nd Mar.—MIDLAND METALLURGICAL SOCIETIES—Open Discussion, "*Some Modern Aspects of Welding*," by A. J. K. Honeyman and A. V. N. Hooker, James Watt Institute, Great Charles Street, Birmingham, 6.30 P.M.

2nd Mar.—WEST OF SCOTLAND IRON AND STEEL INSTITUTE (Joint Meeting with the Engineers Group of The Iron and Steel Institute)—"*A Review of Recent American Rolling Mill Installations*," by A. I. Nussbaum—39 Elmbank Crescent, Glasgow, 6.45 P.M.

2nd Mar.—LIVERPOOL METALLURGICAL SOCIETY—"*Corrosion of Iron and Steel*," by J. C. Hudson—9 The Temple, 24 Dale Street, Liverpool, 7.0 P.M.

2nd Mar.—LEEDS METALLURGICAL SOCIETY—Film on "*Modern Chain Making*," by T. S. Davies—Department of Chemistry, University of Leeds, 7.0 P.M.

6th Mar.—CLEVELAND INSTITUTION OF ENGINEERS—"*Some Aspects of Modern Machine Tools and their Application to Volume Production*," by D. Bailey—Cleveland Scientific and Technical Institution, Corporation Road, Middlesbrough, 6.30 P.M.

6th Mar.—SOCIETY OF ENGINEERS—"*Oxygen and Other Gases and Their Applications to Industry*," by R. E. Doré—Geological Society, Burlington House, London, W.1, 5.30 P.M.

7th Mar.—INSTITUTE OF PHYSICS (Scottish Branch)—"*Optical Methods in the Study of Surfaces*," by S. Tolansky—Natural Philosophy Department, University of Glasgow, 7.00 P.M.

7th Mar.—INSTITUTION OF ENGINEERING INSPECTION (Coventry Branch)—"*Electro-Deposited Surface Finishes for Appearance or Protection*," by A. F. Birchington—Room A5, Coventry Technical College, Coventry, 7.30 P.M.

8th Mar.—EBBW VALE METALLURGICAL SOCIETY—"*Structure of Ingots*," by P. Macnair (Joint Meeting with The Iron and Steel Institute—Corporation Hall, Ebbw Vale, 6.45 P.M.

8th Mar.—NORTH WALES METALLURGICAL SOCIETY—"*New Light on Old Metals : Some Aspects of Atomic Disintegration*," By W. Rogie Angus—County Primary School, Plymouth Street, Shotton, Chester, 7.15 P.M.

13th Mar.—INSTITUTION OF STRUCTURAL ENGINEERS (Scottish Branch)—"*Protection of Steelwork against Corrosion*," by J. L. Wynne—Institution of Engineers and Shipbuilders in Scotland, 39 Elmbank Crescent, Glasgow, 6.15 P.M.

15th Mar.—ROYAL STATISTICAL SOCIETY : INDUSTRIAL APPLICATIONS SECTION, N.E. GROUP—"*Fuel Consumption in a Re-heating Furnace*," by G. H. Jowett—Newcastle Chemical Industries Club, 18 Lovain Place, Newcastle-on-Tyne, 6.30 P.M.

15th Mar.—INSTITUTE OF WELDING (West of Scotland Branch)—"*Some Suggestions for More Efficient Oxygen Cutting*," by E. S. Semper—39 Elmbank Crescent, Glasgow, 7.0 P.M.

15th Mar.—MANCHESTER METALLURGICAL SOCIETY—"*Modern Views on the Corrosion of Metals*," by U. R. Evans—The Engineers' Club, Albert Square, Manchester, 6.30 P.M.

16th Mar.—INSTITUTE OF PHYSICS (Midland Branch)—"*Some Advances in the Theory of Creep in Metals*," by N. F. Mott—The University of Birmingham, Edgbaston, 5.00 P.M.

16th Mar.—SOCIETY OF INSTRUMENT TECHNOLOGY—"*Elements of Automatic Control Theory*," R. S. Medlock—Cleveland Scientific and Technical Institution, Corporation Road, Middlesbrough, 7.30 P.M.

17th Mar.—WEST OF SCOTLAND IRON AND STEEL INSTITUTE—"*Special Steel for Special Purposes*," by J. M. Mowat—39 Elmbank Crescent, Glasgow, 6.45 P.M.

17th Mar.—INSTITUTE OF PHYSICS (Manchester and District Branch)—"*Determination of Phase Boundaries by Magnetic Methods*," by W. Sucksmith—New Physics Theatre, University of Manchester, 7.00 P.M.

18th Mar.—SWANSEA AND DISTRICT METALLURGICAL SOCIETY—"*The Manufacture of Deep Drawing Steel*," by R. W. Evans—Central Library, Swansea, 6.30 P.M.

20th Mar.—CLEVELAND INSTITUTION OF ENGINEERS—"*Oil Refining*," by A. D. Davidson—Cleveland Scientific and Technical Institution, Corporation Road, Middlesbrough, 6.30 P.M.

20th Mar.—SHEFFIELD SOCIETY OF ENGINEERS AND METALLURGISTS—(Joint Meeting with the Engineers Group of The Iron and Steel Institute)—"*A Review of Recent American Rolling Mill Installations*," by A. I. Nussbaum—Royal Victoria Station Hotel, 6.15 P.M.

23rd Mar.—IRON AND STEEL ENGINEERS GROUP—Twelth Meeting—4 Grosvenor Gardens, London, S.W.1, 10.30 A.M.

23rd Mar.—MIDLAND METALLURGICAL SOCIETIES—"*Modern Cast Irons*," by M. M. Hallett. James Watt Institute, Great Charles Street, Birmingham, 6.30 P.M.

24th Mar.—SOCIETY OF INSTRUMENT TECHNOLOGY, (Scottish Section)—"*Instrumentation and Control of Re-heating Furnaces*," by J. Harbinson—Royal Technical College, George Street, Glasgow, 7.0 P.M.

29th–30th Mar.—INSTITUTE OF METALS—42nd Annual General Meeting—Institution of Mechanical Engineers, Storey's Gate, London, S.W.1.

30th Mar.—STAFFORDSHIRE IRON AND STEEL INSTITUTE—"*Cold Rolled Strip*," by B. E. Pollard—Station Hotel, Dudley, 7.0 P.M.

30th Mar.—LEEDS METALLURGICAL SOCIETY—"*Nodular Cast Iron*"—Department of Chemistry, University of Leeds, 7.0 P.M.

30th Mar.—LIVERPOOL METALLURGICAL SOCIETY—"*The Metallurgical Patents of Alexander Parks*," by C. O. Bannister—9 The Temple, 24 Dale Street, Liverpool, 7.00 P.M.

TRANSLATION SERVICE

(The previous announcement was made in the February, 1950, issue of the *Journal*, p. 226).

TRANSLATIONS AVAILABLE

No. 394 (German). W. LUEG : "The Determination of Spread in the Hot-Rolling of Steel with Smooth Rolls." (*Archiv für das Eisenhüttenwesen*, 1949, vol. 20, Jan.–Feb., pp. 443–449 ; discussion, pp. 449–450).

No. 395 (Russian). I. M. CHIZHIKOV : "Evaluation of the Plasticity of Metals and Alloys as Regards Conditions of Hot Shaping by Pressure." (*Zavodskaya Laboratoriya*, 1949, vol. 15, Feb., pp. 191–199).

CHARGES FOR COPIES OF TRANSLATIONS—The charge for the above translations is £1 for the first copy and 10*s*. for each additional copy of the same translation. Requests should be accompanied by a remittance. These translations are not available on loan from the Joint Library.

TRANSLATIONS PREPARED AT MEMBERS' REQUEST—Members requiring translations of foreign papers are invited to communicate with the Secretary, who will ascertain whether they can be prepared for inclusion in the Series.

ABSTRACTS OF CURRENT LITERATURE *and* BOOK NOTICES

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MINERAL RESOURCES

Large-Scale Quarry Operations in Northampton Sand Ironstone. R. B. Beilby. (Iron and Coal Trades Review, 1949, vol. 159, Oct. 14, pp. 901–902). This is an abridged version of a paper read before the United Nations Scientific Conference on the Conservation and Utilization of Resources. The type of strata occurring in Northamptonshire sand ironstone quarries is dealt with, and various factors that require consideration when determining the type of mechanization to be installed, are outlined. These include the shape, size, length, depth of overburden, dip, and methods of dealing with the ironstone.—J. C. R.

The Labrador Iron Range. W. M. Bonham. (Canadian Mining Journal, 1949, vol. 70, July, pp. 57–61). An account is given of the iron ore fields of Labrador. In 1946 the concession holders set 300 million tons of high-grade ore as the minimum that would warrant the heavy investment required to exploit it. Drillings have already proved this amount. The ore is strikingly similar to the Mesabi ores 1200 miles away. Manganiferous ores containing about 7·50% of manganese and 50% of iron are present in large quantities. The construction of a railway to the St. Lawrence River, power supply, operating conditions, and markets are discussed.
R. A. R.

Future Iron-Ore Supplies for the United States. (Iron and Coal Trades Review, 1949, vol. 159, Oct. 14, pp. 905–906). An illustrated account is given of the iron ore deposits of the Labrador Trough which extends from Labrador and is mainly in Ungava or New Quebec. Estimates are given of the size of the deposits and their average analyses. Climatic conditions are reviewed and various markets for the ore are suggested.—J. C. R.

The Iron Ore Industry of Luxembourg. E. H. Elter. (Mine and Quarry Engineering, 1949, vol. 15, Aug., pp. 237–243). A short account is given of the geology of the French–Luxembourg iron ore field, the ore reserves, methods of mining power supply, social conditions, and the trend of production.
R. A. R.

Mining and Economic Problems of the Siegerland Ore Fields. G. Dorstewitz. (Stahl und Eisen, 1949, vol. 69, June 23, pp. 450–452).

The Genesis of the Iron Ore Deposits of the Lahn Type. E. Lehmann. (Zeitschrift für Erzbergbau und Metallhüttenwesen, 1949, vol 2, Aug., pp. 239–246).

Iron-Ore Concentrations near Matmos-Doschatoe on River Oka, Gorkov District. L. M. Miropol'skiĭ and G. L. Miropol'skaya. (Comptes Rendus (Doklady) de l'Académie des Sciences, U.R.S.S., 1948, vol. 59, pp. 303–305 [in Russian]: Chemical Abstracts, 1949, vol. 43, June 10, col. 4189).

Kerch Iron Ore Basin. N. N. Patrikeev and I. S. Shapiro. (Gornyĭ Zhurnal, 1948, vol. 122, No. 7, pp. 7–10: Chemical Abstracts, 1949, vol. 43, May 25, col. 3751). The ore of this region is of three types: brown, black, and tobacco-coloured. The first two were formed from the third by oxidation. Freshly uncovered tobacco-greenish ore is very compact and contains ferrous oxide up to 21% and moisture up to 35%. This ore exposed for a month contains ferrous oxide up to 4·5% and moisture up to 14%. It becomes loose and brown-coloured. The mineralogical composition of the greenish-coloured ore is limonite 50%, chamosite 5%, carbonates of iron, manganese, magnesium, and calcium 10%, phosphates 9%, kaolinite 10%, quartz 5%, silica 9%, gypsum 1%, and sulphides 1%. The brown ore is stable and loose, its colour darkening with the manganese content. The carbonates of iron and manganese of the original greenish ore being changed to oxides, the composition of the brown ore is limonite, oxykerchenite, phosphite, psilomelane, pyrolusite, and wad. The chemical composition of the ore is complicated; as many as twenty-two elements were found in it. For the most important deposits the iron content is 33–40%. The average manganese content is 0·8–4·3%, the average phosphorus content is 0·6–1·1%, and the average sulphur content is 0·1–0·5%. The utilization of the brown and black ores is worked out. The preparation of the greenish ore for smelting is investigated.

ORES—MINING AND TREATMENT

European Experience in Rotary Kiln Nodulizing of Iron Fines and Flue Dust. H. Berg. (Blast Furnace and Steel Plant, 1949, vol. 37, July, pp. 799–801). The use of rotary kilns for nodulizing blast-furnace and iron-ore fines in Europe (*i.e.*, England and the "minette" areas of Luxembourg and Germany) is described.—J. P. S.

Activities of the Spanish Iron and Steel Institute. (Instituto del Hierro y del Acero, 1949, No. 4, Apr.–June, pp. 38–40). [In Spanish]. The activities of the various sections mentioned in earlier journals have continued subject to power restrictions.

The Sinter Committee has begun its investigations with the new pilot plant. It has completed the studies on granulometric and chemical analysis, permeability, porosity, and reducibility of the Spanish, North African, and Franco-Belgian ores mentioned at the meeting of the Sinter Committee last February.

Progress has been made in mining exploration in various Spanish provinces.

One of the most important meetings was that on Industrial Applications of Metallurgical Products in June 1949, when there was much discussion on the standardization of structural steels.—R. S.

Sintering of Iron Ores. P. E. Henry. (Instituto del Hierro y del Acero, 1949, No. 4, Apr.–June, pp. 5–8). [In Spanish]. The author stresses the importance of preparation of materials for sintering, of correct mixture, moisture content, physical condition of the charge operation, and duration of agglomeration. The distinct phases in the operation and the optimum properties of the finished product are described. The author points out that a good sinter is at least as easily reducible as the best ore.—R. S.

Reopening and Developing a Small Red-Iron-Ore Mine, Gadsen, Ala. J. C. Feeley, jun. (United States Bureau of Mines, May, 1949, Information Circular No. 7499).

Concentration of Oxide Manganese Ores from Columbia and Elberta Mining Districts, Tooele and Juab Counties, Utah. (Wildcat, Sharp, and Aeronaut No. 1 Properties). K. C. Dean and K. C. Vincent. (United States Bureau of Mines, June, 1949, Report of Investigations No. 4466).

A New Approach to Blending. J. Meissner. (Blast Furnace and Steel Plant, 1949, vol. 37, Apr., pp. 427–434). The author points out that the common procedure of mixing coal or ores, by feeding them into bins and withdrawing the material at the bottom does not, in fact, effect much mixing and may cause worse segregation. The layer or bed blending system is the only way by which variable coals and ores may be properly mixed. He describes a new enclosed bed system, in which the material is first fed into a long concrete vault, in layers, through a slot in the top: when the vault is full, a reclaimer car moves along the axis of the vault, carrying two helicoidal conveyors, which bring the material to the centre-line of the vault, and an oscillating rake which controls the rate of fall of the material down the working face of the pile. At the centre-line the material falls on to a conveyor in a tunnel. Such a system, it is claimed, requires half the ground area of an open-bed system of similar capacity.

Minerals Concentration. F. T. C. Doughty. (British Engineering, 1949, vol. 31, June, pp. 1968–1976). The author reviews progress made in the separation of minerals from gangue by heavy-media separation, with particular reference to a low-silicon ferrosilicon medium.—R. A. R.

Humphreys Spiral Concentration on Mesabi Range Ores. W. E. Brown and L. J. Erck. (American Institute of Mining and Metallurgical Engineers, Technical Publication No. 2595. Transactions of the American Institute of Mining and Metallurgical Engineers, 1949, vol. 184, pp. 187–193: Mining Engineering, 1949, vol. 1, June, Section 3, pp. 187–193). An account is presented of the use, at the Hill-Trumbull plant of the Cleveland-Cliffs Iron Co. on the Mesabi Range, of the Humphreys spiral concentrator for treating minus ¼ in. iron ore to produce an acceptable concentrate. Operational data are given.—J. C. R.

Sintering Characteristics of Minus Sixty-Five and Twenty Mesh Magnetite. A. Stanley and J. C. Mead. (American Institute of Mining and Metallurgical Engineers, Technical Publication No. 2597; Transactions of the American Institute of Mining and Metallurgical Engineers, 1949, vol. 185, pp. 435–440; vol. 184, pp. 181–186; Journal of Metals, 1949, vol. 1, July, Section 3, pp. 435–440; Mining Engineering, 1949, vol. 1, June, Section 3, pp. 181–186). It is shown from tests that a more accurate metallurgical control on operations will be necessary for sintering minus 65 mesh magnetite than is at present needed in American plants sintering minus 20 mesh magnetite. The plant flowsheet will have to be changed to include the conveying, cooling, screening, storing, and weighing of both return fines and bedding material.—J. C. R.

Rotary Furnace Plants for the Sintering of Blast Furnace Dusts in Luxembourg Works. H. Welter. (Ossature Métallique, 1949, vol. 14, Oct., pp. 445–450). In Luxembourg it is estimated that for a plant producing 1500 tons of iron a day, 50,000 tons of blast-furnace dust are collected each year. This dust contains about 36% of iron, 0·7 % of manganese, and 4 to 5% of carbon. Rotary furnace plants have been set up by various companies to sinter the dust, blast-furnace gas being used to fire the furnaces. At the Differdange plant of the Hadir company it is proposed to charge the furnaces with 80% minette fines and 20% blast-furnace dust. Descriptions are given of these plants at the Belval and Dudelange works of ARBED and the Differdange works of Hadir.—J. C. R.

FUEL—PREPARATION, PROPERTIES, AND USES

Heat Radiation Development. J. DeEulis. (Steel, 1949, vol. 125, Aug. 29, pp. 84, 86). A new radiant tube for heat-treatment furnaces has been developed by Lee Wilson Engineering Co., Cleveland, O. The tube is in the form of an O with an internal air-gas burner; secondary air round the burner accelerates the flame so that heating takes places all round the loop. By varying the secondary air, heating can also be confined to the lower portion of the loop.—J. P. S.

The Metering of Liquid Fuel Flow. J. Stringer. (Journal of The Iron and Steel Institute, 1943, vol. 163, Oct., pp. 168–172). Variations in physical properties cause inaccuracies in the metering of oil with square-edged and sharp-edged orifice flowmeters. A nomogram, given in this paper, facilitates correction for these inaccuracies when the Reynolds number in the orifice lies between 500 and 3000. A means of reducing these inaccuracies, by controlling the temperature of the oil, is discussed and is shown to be possible without undue interference with the requirements of the atomizer and burner. The precautions which must be taken to ensure consistency within ± 1% of indicated flow are stated, and close control of the oil temperature is shown to be necessary. Special orifices, insensitive to viscosity changes, are available, but they are still subject to errors due to specific-gravity changes. The need is emphasized for a continuous viscometer of moderate accuracy as an aid to metering and to furnace control.

Controlling the Quantity of Flue Gas with Gate Valves. M. Hansen. (Stahl and Eisen, 1949, vol. 69, Aug. 4, pp. 563–565). Formulæ are presented for calculating the pressure losses and reductions in flow with different positions of the gate valve in furnace flues, and it is shown how the shape of the flue can be modified in order to increase the response to small movements of the valve.—R. A. R.

The Coal Industry of Brazil. Part I—General Economy, Production and Marketing. J. E. Good, A. Abreu, and T. Fraser. (United States Bureau of Mines, 1949, Technical Paper 713).

A Consideration of the Volatile Matter Content of Bituminous Coal on a Dry Ash-Free Basis. II. J. Hamaker and J. P. Ehrenberg. (Chemisch Weekblad, 1948, vol. 45, Nov. 12, pp. 729–733). [In Dutch].

Preparation Characteristics of Illinois Coals. W. L. Crentz. (United States Bureau of Mines, 1949, Technical Paper No. 724).

Coal Preparation Plants. R. S. Sage. (Blast Furnace and Steel Plant, 1949, vol. 37, July, pp. 813–820). Nearly 30% of the bituminous coal mined in the U.S.A. is cleaned, and 40% is mechanically loaded. The mechanical cleaning of coal involves a large amount of electrical machinery, from 1·5–3·0 h.p./ton/hr. of coal output, consuming 1·0–2·0 kW./ton. The flow sheet for a large modern plant is given in this article and the electrical machinery and control equipment are fully described.—J. P. S.

Froth Flotation of Minus 48-Mesh Bituminous Coal Slurries. D. H. Davis. (Transactions of the American Institute of Mining and Metallurgical Engineers, Coal Division, 1948, vol. 177, pp. 320–337).

The Use of Hydraulic Cyclones as Thickeners and Washers in Modern Coal Preparation. M. G. Driessen. (Transactions of the American Institute of Mining and Metallurgical Engineers, Coal Division, 1948, vol. 177, pp. 240–261).

Coal Washing in Washington, Oregon, and Alaska. M. R. Geer and H. F. Yancey. (American Institute of Mining and Metallurgical Engineers, Technical Publication, No. 2566. Transactions of the American Institute of Mining and Metallurgical Engineers, 1949, vol. 184, pp. 200–204: Mining Engineering, 1949, vol. 1, June, Section 3, pp. 200–204). A summary is presented of coal-washing practice and washability data from plants in Washington, Oregon, and Alaska.—J. C. R.

A Study of Coal Classification and Its Application to the Coking Properties of Coal. M. Perch and C. C. Russell. (American Institute of Mining and Metallurgical Engineers, Technical Publication No. 2584. Transactions of the American Institute of Mining and Metallurgical Engineers, 1949, vol. 184, pp. 205–214: Mining Engineering, 1949, vol. 1, June, Section 3, pp. 205–214). A procedure is described by which criteria used for the standard classification of coals can be arranged so that all coals lie on a single curved line. Coals of different ranks are lined up in accordance with their coking

behaviour and they can be then compared. Exact correlation of coal rank with coking behaviour was not achieved, but with this procedure it is possible to determine the various causes of lack of correlation. Factors such as ash content and differences in amount of petrographic constituents are suggested as causes.—J. C. R.

The Fine Purification of Coke-Fired Producer Gas by Electric Filter. F. Kolbe. (Stahl und Eisen, 1949, vol. 69, Aug. 4, pp. 552–554). The difficulty of cleaning the gas from a coke-fired gas producer is discussed, and an electrostatic cleaner, originally intended for brown coal producer gas, now successfully working on gas from coke is described.

R. A. R.

Electrostatic Removal of Tar from Bituminous-Coal Producer Gas. E. Labouvie. (Stahl und Eisen, 1949, vol. 69, Aug. 4, pp. 554–558). Laboratory and full-scale investigations are described which led to the development of an electrostatic gas-cleaning plant which successfully removed the tar from producers operating on Ruhr bituminous coal which is very high in volatile matter. The plant, operational data for which are presented, consisted of the following units in tandem : precooler, first-stage electrostatic cleaner, indirect final cooler, second-stage electrostatic cleaner, and exhauster. The cleaned gas could be used in gas engines and saleable by-products were recovered so that the plant was an economic proposition.—R. A. R.

Electric Filtering of Blast-Furnace Gas. E. Horoszko. (Hutnik, 1949, vol. 16, Jan.–Feb., pp. 11–17). [In Polish]. The author discusses electrostatic dust precipitation as applied to the cleaning of blast-furnace gas. Single- and double-stage processes, both wet and dry, are compared, and details of operation analysed. The wet electrostatic process is considered the better.—W. J. W.

The Treatment of Water Used for Washing Blast-Furnace Gas. G. Rousse. (Eau, 1948, vol. 35, June, pp. 96–99 : [Abstract] Centre de Documentation Sidérurgique, Bulletin Analytique, 1948, vol. 5, Dec., p. 20).

Notes on Modern Blast Furnace Technique. M. Brun and M. Legendre. (Métallurgie, 1949, vol. 81, May, pp. 13, 15 ; June, pp. 13, 15, 17 ; July, pp. 9, 11 ; Aug., pp. 7, 9–10 ; Sept., pp. 9–11). Continuation of a series of articles (*see* Journ. I. and S.I., 1949, vol. 162, Aug., p. 479). The present parts deal with the cleaning of blast-furnace gas and types of plant used for this purpose.—J. C. R.

The Use of Coke-Oven Gas in the Steel Industry. K. H. Martin. (Stahl und Eisen, 1949, vol. 69, Aug., 4 pp. 558–561). The fuel position of the German steel industry is reviewed, and the data are presented to show the proportions of coke-oven gas, coal, electricity, and other sources of energy used for making steel each month from January 1948 to April 1949. The use of long-distance gas before 1939 and after 1945 is discussed and a long-term policy for the most economic utilization of coke-oven gas is put forward.—R. A. R.

Liquified Petroleum Gases for the Steel Industry. F. E. Drake. (Iron and Steel Engineer, 1949, vol. 26, Aug., pp. 41–55).

TEMPERATURE MEASUREMENT AND CONTROL

The Electronic Measurement and Control of Heat. J. H. Jupe. (Electronic Engineering, 1949, vol. 21, Jan., pp. 13–16 ; Feb., pp. 48–51 ; Mar., pp. 94–97). In Part I three types of electric resistance thermometer with electronic additions, and an electronic apparatus for determining dew-points are described. Part II presents circuit diagrams for photocell and thermocouple methods, incorporating electronic devices, of measuring temperatures above about 530° C., and Part III deals with the application of electronics to the control of electric welding.—P. A. R.

The Radiation Analysis of Liquid Steel. G. Naeser and G. Engels. (Stahl und Eisen, 1949, vol. 69, July 21, pp. 508–514). The authors study means of improving the methods of measuring the radiation from liquid steel, the effects of different alloying elements on the intensity of the radiation, and the relation between the radiation and the quality of the steel. Measurements of the radiation from slag-free and oxide-covered surfaces of liquid steel were made with a total-radiation pyrometer, a photocell, and a Bioptix instrument. These indicated that the measurement of rays close to the infra-red is a suitable method and this would enable a self-recording instrument to be constructed. The effect of small additions of silicon, aluminium, titanium, molybdenum, manganese, or chromium on the radiation from steel is related to the formation of thin oxide films. As to aluminium additions, the best mechanical properties of 57 basic-Bessemer steels were obtained from steels emitting the minimum radiation. Radiation measurements in 20 open-hearth furnace heats supplemented by others from carbon-rod and induction furnaces indicated that the metal becomes brighter just before tapping, and that the degree of brightness is markedly affected by certain additions, particularly ore and fluorspar.—R. A. R.

Automatic Regulation of Temperature and other Quantities Measurable by Galvanometer. G. Milazzo. (Chimica e Industria, 1948, vol. 30, p. 294 : British Ceramic Abstracts, 1949, July, pp. 283A–284A). An apparatus is described for the automatic regulation of temperature and other quantities which can be measured by a galvanometer. It is based on the interruption of a ray of light which excites a photo-electric cell, by an opaque shield integral with the indicating pointer.

The Technique of Temperature Measurement in Foundries. K. Guthmann. (Neue Giesserei, 1949, vol. 36, June, pp. 163–170). Developments in methods of measuring the temperature of liquid metals are reviewed, particularly those in Great Britain and the U.S.A. during the last 15 years. The improvement in the quality of the castings brought about by accurate temperature measurement is stressed.

R. A. R.

Gas Temperature Measurement above 1500° C. R. Mayorcas. (Journal of the Institute of Fuel, 1949, vol. 22, June, pp. 251–255). It is shown that in certain fields of investigation, established techniques of gas temperature measurement above 1500° C. are less satisfactory than newer methods based on an indirect approach. The sensible-heat meter is described ; experiments with it indicate that it gives a simpler and more accurate measurement of the heat contents of gases than other methods based on the measurement of temperature. The sensible-heat meter has the great advantage that, being water-cooled, it can be used safely at the highest temperatures.—R. A. R.

Modern Instrumentation for Measuring Temperature Gradients in Continuous Porcelain Enamelling Furnaces. M. Bozsin and C. A. Vana. (Bulletin of the American Ceramic Society, 1949, vol. 28, June 15, pp. 219–224). A portable three-point electronic temperature recorder is described. This temperature recorder, which is in a heat-insulated box, travels on the conveyor belt of a continuous furnace to record the changes of temperature during the firing cycle, and has three thermocouples to measure the temperature at different levels of the furnace. Results to date show that a close check can be obtained of the firing cycle of a furnace with this instrument.—R. F. F.

REFRACTORY MATERIALS

Forsterite Refractories from Serpentine Materials. N. I. Voronin and A. N. Novikov. (Ogneupory, 1948, vol. 13, No. 9, pp. 397–401 : American Ceramic Abstracts, 1949, Aug., p. 185).

Mechanical Strength of Unfired Magnesite Brick. A. A. Shumilin. (Ogneupory, 1948, vol. 13, No. 8, pp. 349–350 : American Ceramic Abstracts, 1949, Aug., p. 185).

Multigrog Ladle Brick. E. S. Chuprinko and D. I. Gavrish. (Ogneupory, 1948, vol. 13, No. 9, pp. 387–390 : American Ceramic Abstracts, 1949, Aug., p. 185).

Technological Problems in the Firing of Grog in a Rotary Kiln. L. G. Vainrub. (Ogneupory, 1948, vol. 13, No. 8, pp. 339–343 : American Ceramic Abstracts, 1949, Aug., p. 186).

Thermal Characteristics of Lightweight Refractories from the Podol'sk Refractory Works. A. F. Kolechkova and V. V. Goncharov. (Ogneupory, 1948, vol. 13, No. 9, pp. 401–407 : American Ceramic Abstracts, 1949, Aug., p. 186).

Tridymite Dinas from Vein Quartz. V. A. Bron. (Ogneupory, 1948, vol. 13, No. 9, pp. 407–413 : American Ceramic Abstracts, 1949, Aug., p. 186).

Accelerating the Wet Grinding of Refractory Materials. G. V. Kukolev and L. G. Mel'nichenko. (Ogneupory, 1948, vol. 13, No. 10, pp. 447–454 : American Ceramic Abstracts, 1949, Aug., pp. 184–185).

Steel-Tight Linings for Basic Induction Furnaces. J. H. Chesters, L. Lee, and J. Mackenzie. (Transactions of the British Ceramic Society, 1949, vol. 48, Aug., pp. 263–290).

The principles involved in maintaining steel tightness in a refractory lining are reviewed, with particular reference to those peculiar to induction furnace steelmaking. The limitation on the use of basic linings is shrinkage cracking, which allows the metal to strike through to the coil. Control of the expansion characteristics of the lining is considered to be one of the most important features in maintaining steel tightness. Several basic lining compositions are described from the laboratory and practical standpoints. Development work to produce a monolithic lining from sea-water magnesia that would show a permanent expansion in intermediate temperature ranges, and thus give steel tightness within the lining although the working face might be badly cracked by shrinkage, is described in detail. The most satisfactory results were obtained with additions of 4% of sand and 2% of borax to magnesia, expansions of 1·5–2% being recorded at 1150° C. As a result, basic linings of this composition were standardized in the works, with excellent results. Some available commercial linings are described, and also the methods of installation and maintenance. The quality of the raw materials, chemical analysis, grading, and expansion characteristics are considered important in obtaining the best lining. The use of control charts for grading low-temperature expansion at 1150° C. and high-temperature shrinkage is outlined. Some six years of experience on magnesite-sand-borax linings show that close control can be maintained on quality.

Higher Temperature Refractories—Goal of New Research Laboratory. D. Reebel. (Steel, 1949, vol. 125, July 11, pp. 84–86). The General Refractories Co., Philadelphia, has just opened new laboratories at Baltimore. One of the aims of the research programme is the further development of basic refractories for the open-hearth furnace. The equipment includes apparatus for chemical analysis and physical tests on refractories as well as a pilot plant for testing them under service conditions.—J. P. S.

Blast-Furnace Brick Disintegration Test Equipment and Test Procedure. J. A. Shea. (Bulletin of the American Ceramic Society, 1949, vol. 28, July 15, pp. 253–259). Semi-automatic equipment for making disintegration tests on blast-furnace brick lining has been designed, and a suitable testing procedure developed. Essentially, the equipment consists of a gas-tight chamber to hold the specimens, a suitable furnace for heating the chamber, a pump for circulating carbon monoxide over the specimens in a closed system, absorption tubes in the system to remove carbon dioxide and moisture from the circulating gas, and a flowmeter and pressure gauge. In making a disintegration test, the brick specimens in the test chamber are heated to 950° F. in the furnace and then exposed to the action of carbon monoxide flowing at 20 cu. ft./hr. for 40 hr. Operation of the equipment during a test is semi-automatic. Forty hours in this equipment was found to be much more effective than 120 hours in earlier equipment.

Carbon Linings for Blast Furnaces. J. H. Chesters and G. D. Elliott. (Iron Age, 1949, vol. 164, Aug. 18, pp. 89–97). The use of carbon blocks in the blast-furnace hearth is shown to reduce break-outs, and in the stack, to prevent scaffolds. In comparison with fireclay bricks they show less shrinkage in service, have a higher thermal conductivity, are not wetted by the molten iron, and are inert to blast-furnace slag and alkalies: above red heat, however, they react readily with oxygen, water vapour, and carbon dioxide, They have a high refractoriness-under-load, and are apparently immune to carbon monoxide disintegration. For these reasons a hearth of carbon blocks will retain its contours for a long time; one especial reason for this is that the high conductivity of the material renders the water-cooling more effective. The methods used at Appleby Frodingham in the construction of a carbon hearth are described; the hearth, of an inverted dome shape, is constructed of two courses of blocks of special locking corrugated form, laid dry without carbon paste cement.—J. P. S.

Refractory Practice in the Whiting-Cole Recirculating Annealing Oven. R. A. Witschey. (Foundry, 1949, vol. 77, Sept., pp. 80–83, 244–245). An illustrated account is presented of the refractory lining of Whiting-Cole recirculating periodic annealing furnaces. Details are given of variations made in the type of refactory material used in the construction of different ovens.—J. C. R.

Effect of Oxygen on Refractories in the Basic Open Hearth. R. S. Moore. (Iron and Steel Engineer, 1949, vol. 26, July, pp. 47–50). The author reviews the present state of knowledge on the use of oxygen in basic open-hearth furnaces and its effect on the life of refractory materials. Oxygen itself does not have any deleterious effect on these refractories but it does have indirect effects, as, for example, the increased concentration in the furnace atmosphere of iron oxide which has a fluxing effect on silica brick and fireclay. In reference to roof bricks, the increased resistance to attack of super-duty silica bricks over ordinary silica bricks and their cheapness as compared with chrome-magnesite bricks are discussed.
J. C. R.

Mechanism of Erosion of Nozzles in Open-Hearth Ladles. R. B. Snow and J. A. Shea. (Journal of the American Ceramic Society, 1949, vol. 32, June 1, pp. 187–194). A detailed report is presented of experiments made with removable nozzle extensions to determine the relative amount of erosion when various types of steel are teemed through (1) regular fireclay nozzles fired to different temperatures and (2) nozzles of the more refractory types. The mechanism of erosion is discussed. The data indicates the limitation of refractory materials of Seger cone 28 or higher for use in nozzles or as open-hearth ladle brick.

Study of Cements for Mixers Based on Magnesium Oxychlorides and Oxysulphates. P. Rocquet. (Revue de Métallurgie, Mémoires, 1949, vol. 46, Sept., pp. 639–644). Arising out of the assertion by steelmakers that mixers with linings in which magnesium oxysulphate cement was used had a longer life than those in which magnesium oxychloride had been used, the author studied the decomposition of such cements during heating up to 1250° C. and compared their fritting behaviour. The mixtures were intended to set in an hour and to produce cements that would not crack. The different behaviours of the two types of cement is attributed to the difference between the temperature of complete elimination of the chloride and the sulphate. There is a temperature gradient from the inside towards the outside of the vessel. The last of the chlorine is driven off by about 900° C. but as fritting does not set in until, say, 1250° C. there is a large thickness of friable cement under the fritted zone. With the oxysulphate cement, complete elimination is achieved only at 1200° C., so that the friable zone between 1200° and 1250° C. is much smaller. The sublimation of magnesium chloride may lead to greater porosity than does the decomposition of the oxysulphate. Other tests made covered the mechanical properties, setting times, and the grade of burnt magnesia, including used magnesite bricks, to be used in making the cements.—A. E. C.

Refractory Materials for Cupolas, Converters and Ladles. F. Klein. (Neue Giesserei, 1949, vol. 36, June, pp. 170–172). The most suitable refractory materials for a steel foundry using cupolas, converters, and ladles, and their properties are reviewed and discussed.—R. A. R.

The CaO–MgO–Cr_2O_3 Ternary System. Part II—Further Experiments. W. F. Ford and W. J. Rees. (Transactions of the British Ceramic Society, 1949, vol. 48, Aug., pp. 291–321). Part I of this work appeared in Transactions of the British Ceramic Society, 1948, vol. 47, June, pp. 207–231 (*see* Journ. I. and S.I., 1948, vol. 159, Aug, p. 433). In this paper the general properties of calcium chromate, the '941' compound, and calcium chromite and the nature of the ternary system were studied. An X-ray study of the dissociation of calcium chromate *in vacuo* has produced evidence for the constitutions of the complex compounds formed in air below liquidus temperature and indicated the mechanism of oxidation of calcium chromite. The true binary compound $CaO.Cr_2O_3$ occurs in two forms, a high-temperature or alpha form which has simple tetragonal symmetry and the low-temperature or beta form which occurs as orthorhombic needles. The phase distribution in the CaO–MgO–Cr_2O_3 has been determined in air at various temperatures and *in vacuo* and it is shown that two-thirds of the diagram is of low refractoriness. The application of the diagram to slag practice in the manufacture of chromium steels is also discussed. In an appendix J. White interprets experimental data relating to this system.
R. F. F.

Protective Coatings—Glazes. A. K. Karklitt and A. I. Gavrilov. (Ogneupory, 1949, vol. 13, No. 9, pp. 413–415. American Ceramic Abstracts, 1949, Aug., p. 185). Compositions and characteristics of five experimental protective

coatings are given. The products were intended for facing brickwork in boiler plants using liquid fuels and operating in the 1300–1500° C. range. One of the materials proved to be successful in extensive tests.—R. A. R.

Use of Enriched Ash of Moscow Coal in the Production of Lightweight Refractories. D. N. Poluboyarinov and E. V. Mirolyubova. (Ogneupory, 1948, vol. 13, No. 10, pp. 435–442 : American Ceramic Abstracts, 1949, Aug., p. 186).

The Use of Lightweight Refractories in Furnace Construction. M. J. Molinié. (Journal des Usines à Gaz, 1948, vol. 72, p. 179 ; British Ceramic Abstracts, 1949, Aug., p. 314A). A simple exposition is given of the advantages of insulating refractories as constructional material for intermittent furnaces. An analogy is offered by the power consumption in driving a railway train, the dead-weight of the train, which has to be first accelerated and then brought to a standstill again, being similar to the thermal capacity of the furnace.

BLAST-FURNACE PRACTICE AND PRODUCTION OF PIG IRON

Methods for Reducing the Amount and Quality of Coke Used in Smelting Iron Ore. P. E. Cavanagh. (United Nations Scientific Conference on the Conservation and Utilization of Resources. Lake Success, Aug. 17–Sept. 6, 1949 ; Advance Copy.) The author discusses methods alternative to the use of the blast-furnace for producing pig-iron, such as the Tysland-Hole low-shaft furnace, the Wiberg Söderfors sponge iron furnace, and small blast-furnaces employing sinter. He considers that these are only of significance where a local market exists, too small for a full-size blast-furnace, and where the cost of imported pig iron is greater than the production cost by these methods. The hope of important coke conservation in iron smelting lies in the use of higher top pressure and greater blast volume, and, in the more distant future, in the use of oxygen-enriched blast which will permit the employment of lower-quality coke.—J. P. S.

Conservation of Iron and Steel in Production : Ideas on the Future Production of Iron and Steel. R. Durrer. (United Nations Scientific Conference on the Conservation and Utilization of Resources, Lake Success, Aug. 17–Sept. 6, 1949 ; Advance Copy). The use of oxygen-enriched blast in the smelting of iron ore will permit (1) the construction of a lower shaft furnace, allowing the use of softer cokes and ores, and (2) the smelting of leaner ores with lower grade fuels.—J. P. S.

The Oxidizing Zone when the Blow is Oxygen-Enriched. I. Z. Kozlovich. (Stal, 1947, vol. 7, pp. 587–592 [in Russian] : Chemical Abstracts, 1949, vol. 43, Apr. 25, cols. 2907–2908). To study the changes in the oxidizing zone ore tuyere of a blast-furnace was adapted to use with oxygen-enriched air. The concentration of oxygen was varied from 21 to 33·6%. As the concentration of oxygen increased, the flame focus moved closer to the tuyere, the depth (along the furnace radius) of the oxidation zone shortened, and the maximum CO_2 content rose. Thus at 21% of oxygen the flame focus was 400 mm. from the tip of the tuyere, the oxidation zone extended to 1100 mm. (along the radius) from the tuyere, and the maximum CO_2 content was 14·3%. At 33·6% of oxygen the respective values were 250 mm. (estimated), approximately 700 mm. and 27·8%. The CO content up to the flame focus remained low and practically unchanged ; beyond, it increased sharply with the oxygen content in the blow.

Smelting with Oxygen-Enriched Blast. H. Hellbrugge. (Stahl und Eisen, 1949, vol. 69, Apr. 14, pp. 256–258 : Iron and Coal Trades Review, 1949, vol. 158, May 20, pp. 1123–1125). Pig iron and ferro-alloys were successfully made in a low-shaft furnace 3 m. in outside dia. and 7 m. high using oxygen-enriched unheated blast. The furnace lining was of carbon up to the tuyere level, of magnesite bricks for the lower half of the shaft, and of firebricks for the upper half. The indirect reduction during the production of basic iron was better than expected. The tendency for reduction with CO increased with increasing quantities of CO in the top gas. Oxygen smelting is accompanied by less indirect reduction and more heat in the waste gases. A high carbon ferro-chromium (with 28 to 32% Cr) was successfully produced, and, with 80% oxygen in the blast, the furnace has made ferro-silicon–chromium (47% Cr, 13% Si).—R. A. R.

Electric Smelting of Indian Iron Ores. H. Christiansen. (Transactions of the Indian Institute of Metals, 1949, vol. 1, Apr., pp. 45–52). Two 13,200-kVA. electric pig iron furnaces of the Tysland-Hole type are to be erected at the Mysore Iron and Steel Works, Bhadravati. The design of the plant is described.—R. A. R.

Producing Synthetic Cast Iron in the Electric Furnace. E. Zan. (Metallurgia Italiana, 1949, vol. 41, May–June, pp. 144–148). The production of synthetic cast iron in the electric furnace, using steel scrap, with coke and electrode scrap as carburizing agents, is illustrated. The relation of the carbon taken up by the melt to that in the charge is discussed, and the characteristics of the cast irons obtained (engineering cast irons and hematite irons for ingot moulds) are described.—R. F. F.

The Technology of the Three-Phase Electric Furnace for Reducing Ores, and Estimating its Economy. G. Keller-Wylenmann. (Berg- und Hüttenmännische Monatshefte der Montanistischen Hochschule in Leoben, 1949, vol. 94, June, pp. 129–134). The fundamentals of the low-shaft electric furnace process for reducing iron ores are pointed out and the economy of the process is compared with that of the coke blast-furnace. The higher temperature of the slag in the electric furnace enables it to take up more sulphur and silica ; iron with less than 0·02% sulphur can be made from a high sulphur ore and a low-silicon iron from a siliceous ore. The gas from the electric furnace is high in carbon monoxide and has a calorific value of 2200–2600 cal./cu. m. as compared with 700–900 for blast-furnace gas. The values of the slags produced by the two processes are approximately the same, but the metal/slag ratio is much higher in the electric process. In estimating the economy of the electric process it is wrong to consider only the ratio of the price of coking coal to that of electric current as many other factors must be examined. R. A. R.

On Reducing Aluminium–Iron Ores (Bauxites). B. Vécsey. (Bányászati és Kohászati Lapok, 1949, vol. 4, May 15, pp. 186–194). [In Hungarian]. There are no deposits of iron ore in Hungary and great efforts are being made to develop a satisfactory process for extracting iron from bauxite. A small blast-furnace of 50 cu. m. volume has been built at Diósgyör and experiments are being carried out there with charges containing various percentages of scrap and bauxite. (*See* ' Production of Grey Iron from Iron and Steel Swarf and Bauxite in Hungary,' Journ. I. and S.I., 1949, vol. 162, Sept., p. 94). The author tried to reduce mixtures of aluminium oxide and iron oxide in CO–CO_2 gas streams at various temperatures, and found that bauxite high in iron is very difficult to reduce even at high temperatures ; at 900° C. he obtained an intermediary product which could only be reduced further with carbon. It appears that indirect reduction of bauxites in the upper part of the blast-furnace only yields FeO, and further reduction requires carbon. For this reason much more coke is required for bauxite ores than for other types of iron ore.—E. G.

Investigations on an Experimental Blast-Furnace. H. L. Saunders, G. B. Butler, and J. M. Tweedy. (Journal of the Iron and Steel Institute, 1949, vol. 163, Oct., pp. 173–206). Details are given of the general layout and design of an experimental blast-furnace, together with details of tuyeres, blast heater, sampling probes, and compensated charging mechanism. The main object of the campaign was to determine the manner in which temperature and CO_2 distribution in the stack were influenced by changing the segregation of the ingoing materials. Heat balances have been drawn up for each of the trial periods and a general interpretation of the experimental data in relation to the blast-furnace process has been attempted.

The Thermodynamic Background of Iron and Steel Making Processes. I—The Blast-Furnace. F. D. Richardson and J. H. E. Jeffes. (Journal of The Iron and Steel Institute, 1949, vol. 163, Dec., pp. 397–420). A survey is made of the thermodynamic data available for the main compounds of importance in ironmaking. The free energy and heat changes of the most important reactions have been calculated up to 2000° C. and the manner in which they vary with temperature is shown by means of suitable diagrams on which the probable limits of accuracy are indicated. The results are discussed and are applied to the processes occurring in the blast-furnace, in terms of oxygen potential. The deductions concern the effects of temperature, pressure, and concentration, on the interlocking of many simultaneously occurring reactions.

A New Blast-Furnace Stock-Rod Gas-Seal. E. J. Walklate. (Journal of The Iron and Steel Institute, 1949, vol. 163, Dec., p. 432). The stock-rod gas-seal described consists of an outer casing 12 in. long and 7½ in. in dia., containing an inner floating sleeve packed with fine steel wool and grease. A nipple for injecting grease is provided. The life of the stock-rod with this type of seal is about four months.—R. A. R.

The Gas Velocity in the Blast-Furnace Shaft. M. Hansen. (Stahl und Eisen, 1949, vol. 69, Sept. 15, pp. 676–677). In 1929 S. P. Kinney used a cylindrical probe with two holes to determine the velocity of the gases at different positions in the blast-furnace (*see* Journ. I. and S.I., 1929, No. II, p. 421). The Kinney probe has recently been recalibrated and errors have been found which are dealt with in the present paper, correction factors being presented.—R. A. R.

Use of Enriched Air for Blast Furnace Operation. R. Rocca. (Metallurgia Italiana, 1946, vol. 38, Apr.–June, pp. 35–44).

The Development of Electric Iron Smelting. H. U. Ross. (Canadian Mining Journal, 1949, vol. 70, July, pp. 66–70). A brief account is given of the development of the Héroult, Electrometall, Tysland, and Tysland-Hole electric furnaces and their use for producing pig iron. The Tysland-Hole furnace would be suitable for certain parts of Canada where the demand for pig iron is not great, where coking coal is unavailable and hydro-electric power is plentiful.—R. A. R.

Electric Iron-Ore Smelting. M. A. Kassem. (Metallurgia, 1949, vol. 40, July, pp. 145–149). The Tysland-Hole electric iron-ore smelting furnace is described in detail. The economic utilization of the furnace gas which has a calorific value of 2400 kg.cal./cu. m. is considered, accounts of its use in cement manufacture and to achieve additional reduction by such 'direct' processes as a suitably modified Wiberg process being given.—R. A. R.

Blast Furnace Practice at Fontana. C. H. Lenhart. (Iron and Steel Engineer, 1949, vol. 26, July, pp. 35–46). A general account is given of attempts to improve blast-furnace operations at the Fontana, Cal., works of the Kaiser Co. Inc. Particular reference is made to the quality of the coke used and beneficiation of the iron ores.—J. C. R.

Basic Linings for Ladles. Benefits in Sodium Carbonate Desulphurisation Process. F. Brunt and H. Parnham. (Iron and Coal Trades Review, 1949, vol. 159, Sept. 9, pp. 631–634). An account is given of experimental work undertaken to investigate the possibility of using basic-lined ladles in the sodium carbonate desulphurization process. Tests with 100% dolomite brick gave a much longer life than firebrick and achieved much better sulphur removal. Ladles used with basic linings should have a minimum of taper to enable the number of shapes of the brick and the amount of jointing material required to be kept to a minimum.—J. C. R.

Effect of Acid Oxides upon Desulphurization in the Iron–Manganese System. M. B. Royer and A. J. Williams, jun. (United Bureau of Mines, 1948, June, Report of Investigations, No. 4300). Test melts in crucibles were made to study the limits of the solubility of sulphur in melts of electrolytic iron and electrolytic manganese, and the desulphurizing action of acid oxides. The concept of desulphurization by acid oxides was not substantiated. These oxides are only effective for desulphurizing to the extent to which they react with a constituent of the metal bath, yielding a slag of the proper composition with the fluidity necessary for dissolving sulphides at the normal operating temperatures.—R. A. R.

TREATMENT AND USE OF SLAGS

Paving Blocks from Blast-Furnace Slag. I. S. Vlodavskii. (Stal, 1947, vol. 7, pp. 940–941 [in Russian]: Chemical Abstracts, 1949, vol. 43, June 10, col. 4197). Road paving blocks are made of blast-furnace slag poured directly from the slag thimbles into prepared moulds. Details of the operation are given.

The Working-Up of Crushed Slag from Slag Heaps for the Production of Hydraulic Binders. G. Mussgnug. (Stahl und Eisen, 1949, vol. 69, Apr. 28, pp. 301–306). Investigation of slags from slag heaps at four German ironworks showed that the long period in the open had not reduced the hardening capacity of crushed blast-furnace slag; in many cases it had actually improved, the cause of this being the activation of part of the silica. With manganese-bearing slags the formation of hydrates by the higher oxides of manganese also contributes to the hardenability. Material from old slag heaps is characterized by the high loss on ignition and its high SO_3 content as compared with new slag. Not quite all of the water of hydration can be driven off by ordinary drying methods and drying by a counter-current process is recommended. The slag is hard to grind and the high degree of fineness means a heavy consumption of power.—R. A. R.

Extending the Exploitation of Blast-Furnace Slag. H. Schumacher. (Stahl und Eisen, 1949, vol. 69, May 26, pp. 372–378). The plant to make cement binder and other building materials from blast-furnace slag heaps at the Thyssen works is described. A Dwight-Lloyd sintering plant was used to make 'sinter pumice' from a mixture of 80% ground slag and 20% boiler ash. The sinter pumice is crushed and screened in size ranges and it is then used as raw material from which bricks and roofing tiles are made.—R. A. R.

Fluidity of Blast-Furnace Slags at Low Temperatures. A. I. Zhilin. (Comptes Rendus (Doklady) de l'Académie des Sciences, U.R.S.S., 1948, vol. 59, pp. 293–294 [in Russian]: Chemical Abstracts, 1949, vol. 43, June 10, col. 4195). At only 950–1040° (measured by optical and thermoelectric methods) a surprisingly low viscosity is observed in foamy slags during industrial casting. The same 'dry' slag solidifies at 1225–1300°; the introduction of superheated water vapour for the production of foamy slag reduces the viscosity considerably. The crystallization of this slag is done in moulds at about 950°. Melilite is the chief mineral in the finished product, besides gehlenite and pseudo-wollastonite. The crystallization usually starts from gas bubbles. The chemical analysis of five typical slags shows retained water contents ranging from 0 to 0·85%. The crystallization of the foamy slags begins in the steam chamber and continues over the cooling period in the moulds. Temperatures of 910–890° were measured by thermocouples introduced into the centre of the blocks.

PRODUCTION OF STEEL

Aliquippa Works, Jones and Laughlin Steel Corporation. C. Longenecker. (Blast Furnace and Steel Plant, 1949, vol. 37, Aug., pp. 927–959). The Aliquippa Works was built as an integrated steelworks with coke ovens, sintering plant, blast-furnaces, converters and open-hearth furnaces, soaking pits, blooming, billet, and rod mills. Expansion has been considerable since then, and to ensure coal and ore supplies the company has installed gear at its own mines for washing and beneficiating coal, and has purchased new iron-ore mines in the Adirondacks. This paper describes in considerable detail the coke-oven plant, the blast-furnaces, the Bessemer and open-hearth departments, the blooming, billet, bar, and finishing mills, the strip mills, the mill for rolling rounds for subsequent piercing into seamless tubes, the piercing mill, the butt- and lap-welding mills, and the power generation, electrical, and maintenance departments.—J. P. S.

Expansion Program Increases Capacity at Bethlehem's Sparrows Point Plant. (Steel, 1949, vol. 25, July 18, pp. 102, 104, 107, 122). New equipment installed at the Sparrows Point plant of the Bethlehem Steel Co. over the past 11 years includes 122 new coke ovens, with another 65 under construction; accompanying by-products plant is on a similar large scale. Two new blast-furnaces were installed in the period; one of these, blown-in in 1948, recently established the worlds record make of 1753 tons/day. Five new open-hearth furnaces were added. A new 68-in. continuous hot strip mill and a four-high four-stand tandem cold reduction mill of 56 in. capacity were installed in 1947, together with continuous pickling lines and coil-annealing furnaces. Plant for bar and wire production has also been increased during the period under consideration.—J. P. S.

Manganese Economy in Basic-Bessemer Steelworks. J. Klärding. (Stahl und Eisen, 1949, vol. 69, Sept. 29, pp. 691–695). This paper reports data on the consumption, recovery, and costs of manganese in basic-Bessemer practice in Germany and compares the results for different forms of addition. Economy was effected by increasing the proportion of ladle additions.—R. A. R.

Survey of Open Hearth Alloy Steel Off-Heats and Residual Elements. (American Iron and Steel Institute, 1949, Sept., Contributions to the Metallugy of Steel, No. 35). Data are presented in tabular and graphical form which show the numbers of heats in which the actual contents of the usual elements (C, Mn, Si, P, S) and of alloying elements fell outside the intended range. The data were collected from nearly

14,000 heats and are given as totals and as percentages for each element. Open-hearth furnace residual nickel, chromium, and molybdenium contents are also given.—R. A. R.

The Problem of Charging Scrap in Modern Open-Hearth Steel Plants. E. L. Diamond. (British Science News, 1949, vol. 2, No. 22, pp. 298–301). Investigations by the British Iron and Steel Research Association of the flow of materials for charging open-hearth furnaces are reported. The use of a model and charts on a time basis have been helpful. The ideal procedure is to charge continuously and uniformly at such a rate as to keep the roof temperature just at its maximum while the maximum rate of fuel input is maintained. A rotating bench-loading machine for filling charging pans is proposed; it has the merits of storing a complete furnace charge in the minimum floor space behind the furnace, and of being able to replace a row of empty pans by a row of full pans in a few seconds without interrupting the charging machine.—R. A. R.

Oxygen in Steel Producing Furnaces. G. D. Stone. (Iron and Steel Engineer, 1949, vol. 26, Aug., pp. 56–58). Some observations on the use of oxygen in steelmaking furnaces are reported. Oxygen was used (1) for combustion during the meltdown and occasionally for refining in the open-hearth and (2) for decarburization in both the open-hearth and the electric furnace. Improved heat transfer to the cold charge occurred when the oxygen stream was directed below the stream of atomized oil. Best results are obtained if oxygen is used after the charge has reached 2000° F. and continues until the charge is completely melted. The effects of injecting oxygen into the molten bath are summarized as: (1) acceleration of carbon elimination; (2) increase of bath temperature: (3) speeding the start of the lime boil in basic furnaces. The effects of varying oxygen purity, preheating air, decarburization in electric furnaces, and means of injecting oxygen into the bath, are briefly discussed.—J. C. R.

Carburetting Tests with Pulverized Coal in the Coke-Oven-Gas-Fired Open-Hearth Furnace. E. Meier-Cortés. (Stahl und Eisen, 1949, vol. 69, July 7, pp. 476–480). Owing to the poor quality of coke-oven gas at a plant with two 40-ton open-hearth furnaces, carburetting with pulverized coal was tried and found successful. A crusher with a capacity of 300 kg./hr. was used. Of the product, 85% passed a screen of 4900 mesh/sq. cm. Data on the ash deposited in the checker chambers are presented. The average consumption of coal was 12 kg./1000 kg. of ingots produced.—R. A. R.

Electric Ingot Process Saves Strategic Alloys. E. S. Kopecki. (Iron Age, 1949, vol. 164, Aug. 18, pp. 81–87). The Kellogg process (*see* Journ. I and S.I. 1948, vol. 159, May, p. 88) is discussed; it is considered that the recovery of alloying elements is of a high order and that from this point of view, and also because the ingots have no pipe, the process is superior to the arc- and induction-furnace processes.—J. P. S.

Making Steel from Argentine High-Phos Pig Iron. E. J. Ash. (Steel, 1949, vol. 125, Aug. 8, pp. 78–80). The pig iron produced in Argentina from local ore contains up to 1·20% of phosphorus and has been, up to the present, used solely by foundries. Steel is made only from scrap, and scrap supplies are diminishing. A trial was therefore run on 3 tons of Argentine high-phosphorus pig iron by the Timken Roller Bearing Co., at Canton, Ohio: a 1-ton basic electric furnace was used. The phosphorus was worked down with lime, mill-scale, and iron ore, decarburization being helped with oxygen. The final phosphorus content obtained was 0·03% and the physical properties of the steel were satisfactory. J. P. S.

Pilot-Plant Production of Steel from Sponge Iron. W. W. Stevens and J. L. Morning. (United States Bureau of Mines, 1949, Aug., Report of Investigations, No. 4498). The problems and costs involved in producing steel from sponge iron charges in a 4-ton basic-lined arc furnace were investigated. Major difficulties encountered were excessive slag volume, violent boiling action during melt-down, and bridging by the briquettes welding together in the furnace. Improvements were obtained by adding enough carbon to the briquettes to complete reduction of the iron oxide in the sponge. Equations are given for calculating the amount of reducing agent, the weight of slag when melting sponge iron of a given analysis, and the electric power requirements.—R. A. R.

Steel and Slags, the Laws of Their Mutual Reactions. W. Oelsen. (Berg- und Hüttenmännische Monatshefte der Montanistischen Hochschule in Leoben, 1949, vol. 94, Aug.–Sept., pp. 232–237). Diagrams showing (*a*) the iron corner of the Fe–Mn–O constitutional diagram, (*b*) diagram for the equilibria between iron melts and iron-manganese silicates in a sand crucible at 1600° C., and (*c*) the iron corner of the iron–oxygen–carbon diagram are presented. The general conclusions drawn from these, taking also J. W. Grieg's work into account are: (1) In liquid slags consisting of basic and acid oxides there occur in the middle region compounds having properties quite different from those of the basic and acid oxides on each side. (2) The transition from basic to acid oxides in the melt does not take place continuously because the difference in the properties of the compounds is so great that dissociation (or a strong tendency thereto) takes place on each side of the middle region. This dissociation is very marked on the side of the basic oxide compounds when a weak base (*e.g.*, FeO or MnO) is melted with the compounds of a strong base (*e.g.*, $3CaO.P_2O_5$). The importance of including the sulphides into these considerations is pointed out.—R. A. R.

Surface Tension and Viscosity of Synthetic Slags. P. Kozakevitch. (Revue de Métallurgie, Mémoires, 1949, vol. 46, Aug., pp. 505–516; Sept., pp. 572–582). The paper is in two parts dealing respectively with binary mixtures and with ternary mixtures, and slag foams and emulsions. The first part describes the determination of the viscosity by the co-axial cylinders method and the surface tension by the pull-on-cylinder method. The slags consisted of FeO in which various other oxides were dissolved. The viscosity–temperature curves consisted usually of two straight lines, their point of intersection corresponding to the commencement of crystallization. Silica raised the viscosity slowly until present in a proportion corresponding to the orthosilicate, then rapidly. A weak maximum on the viscosity–composition curve corresponded to fayalite. SiO_2, TiO_2, Na_2O, P_2O_5 and MnO reduced the surface tension of FeO, whilst Al_2O_3 raised it a little. The curve relating the molecular content of the solute to the surface tension for the FeO–CaO system passed through a minimum. P_2O_5 and Na_2O dissolved in molten FeO exhibited true surface activity. In ordinary open-hearth slags with little P_2O_5, the only component able to depress the surface tension notably is SiO_2; further the surface layer is always rich in absorbed silica, hence it may have an enhanced superficial viscosity.

The second part records the determination of the viscosity of a large number of slags in the FeO–CaO–SiO_2 and the FeO–MnO–SiO_2 systems at temperatures ranging from 1450° C. to the commencement of solidification. Iso-viscosity curves for 1400° C. are given. A weak indication of the existence of $FeCaSiO_4$ was noted, but no evidence of compounds in the system FeO–MnO–SiO_2 was found. SiO_2 increased the viscosity of these series slowly at first and then rapidly. The surface tensions of slags in the foregoing systems and also in the FeO–Na_2O–SiO_2 system were determined. Lime increased the surface tension of FeO–CaO–SiO_2 slags by withdrawing SiO_2 from the superficial film to form inactive calcium silicates. SiO_2 decreased the surface tension of FeO–MnO–SiO_2 slags almost independently of the FeO : MnO ratio; its effect in a given FeO–CaO mixture was small unless it was present in quantities sufficient to form 'free' silica, resulting from the dissociation of the lime silicates formed. The surface tension of open-hearth slags with low P_2O_5 contents can be calculated from the FeO–CaO–SiO_2 and FeO–MnO–SiO_2 diagrams. The surface activity of Na_2O was not diminished by additions of SiO_2; this fact and the surface activity of P_2O_5 suggest that silicates and phosphates, if they form, are only slightly dissociated electrolytically. The surface activity of Cr_2O_3 in acid siliceous mixtures suggests that the ties between the metallic ions and the SiO_4 aggregates are strong enough to make them move together rather than separately (except in the case of electrolysis). The stability of slag foams low in P_2O_5 is due to adsorbed silica; numerous experiments on open-hearth slags confirmed that the stability was independent of the $CaCO_3$: Si ratio. TiO_2 reduced the viscosity only if the slags were acid, *i.e.*, the silica content exceeded that corresponding to the orthosilicate. The surface films were almost always acid; TiO_2 made them more fluid, more effectively even than the slag itself. Bibliographies of 40 and 33 references are appended to the two parts of the paper.—A. E. C.

The Control of Open-Hearth Slags According to the *p*H Value of the Aqueous Suspension. M. S. Kovtun and A. D.

Ogorodnyaya. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 994–996). [In Russian]. A method for the rapid estimation of the basicity of open-hearth slags is described in which the *p*H of an aqueous suspension of 250–300 mg. of the slag was compared with that of a buffer solution of known *p*H. The slag sample was crushed to pass through a 40-mesh sieve and shaken with 100 ml. of freshly distilled and reboiled water. After allowing to settle for 5 min., the *p*H was matched electrometrically with one of 12 buffer solutions with *p*H values ranging from 7·8 to 12·98. A linear relationship was found to exist between the basicity (CaO/SiO_2) of the slag and *p*H of the suspension. The time of a basicity determination by this method was 20 min.—S. K.

Experiments with Small Ingots of Rimming Steel. D. Binnie. (Journal of The Iron and Steel Institute, 1949, vol. 163, Oct., pp. 159–167). Ingots of rimming steel, up to 3 cwt. in weight, were teemed into hematite moulds, copper moulds, and into moulds partially lined and fully lined with refractory material, with the object of comparing the effect of chill and refractory lining on the rimming action. The opening section of the paper confirms the validity of using these small ingots for the study of the rimming action. It was found that refractory-lined moulds reduced the depth of the rim to about one-half that obtained when chill moulds were used. The chemical analysis of the rim was purer, and the core analysis was also purer and steadier, when refractory-lined moulds were used. Hematite moulds and copper moulds gave no appreciable difference in the depth of the rim. Chemical analysis shows that the refractory-lined mould is to be recognized in preference to the iron mould in general use for the manufacture of rimming steel.

Teeming Practice. Overcoming Difficulties in the Production of Good Ingots. N. H. Bacon. (Iron and Steel, 1949, vol. 22, Aug., pp. 372–376; Sept., pp. 417–421). A detailed review of modern teeming practice is presented. Among the subjects discussed are nozzles, stopper ends, direct teeming, tundish teeming, and bottom pouring.—J. C. R.

Defects in Ingots and Their Relationship to Casting Temperature and Pouring Speed. F. Beitter. (Stahl und Eisen, 1949, vol. 69, Aug. 18, pp. 585–600). Defects in ingots are discussed; sixteen types of defect, the causes, and preventive measures are listed. The occurrence of longitudinal cracks and scabs is related to the casting conditions in a manner which indicates the optimum conditions for preventing their occurrence. The effect of oxide coatings on molten steel and the reasons for the development of quite different casting techniques are dealt with.

A method of calculating the melting point of ferrous materials from their composition is explained. Works data and the literature are drawn upon to construct frequency curves for the degrees of superheat in tapping and casting temperatures and from these the recommended temperatures for the prevention of defects in different steels are deduced.

The fact that the evolution of gas from steel governs the time of formation of a surface skin of oxide has led to a new process for purging gas from steel in which a small quantity (about 0·02%) of a low-melting-point heavy metal (lead or bismuth) is added before or during casting; the continuous evolution of metal vapours at high pressure effectively cleans the steel.—R. A. R.

FOUNDRY PRACTICE

Three-Inch Cupola Aids Study of Carbon Absorption. (American Foundryman, 1949, vol. 15, May, p. 62). An account is given of operating technique with a very small cupola (3 in. inside dia.) at McWane Cast Iron Pipe Co., Birmingham, Ala., U.S.A., which is used as a laboratory instrument to determine the effect of various elements in iron on the ability of the iron to absorb carbon.—J. C. R.

Coke Quality. Its Effect on Cupola Operation. W. J. Driscoll. (Iron and Steel, 1949, vol. 22, July, pp. 349–353). The effects of variations of certain properties of coke on the working of the cupola are reviewed to enable a better understanding of the desirable properties of cupola coke to be obtained. It is suggested that a suitable coke might conform to the following recommendations: (1) It should be available in the screened condition, the extremes of the size range being 2 in. and 8 in. In any one size-grade the larger screen should be 1½–2 times the smaller screen size. In any grade the coke as delivered should not contain more than 10% by weight with a size less than 2 in.; (2) the 2 in. shatter index should not be less than 85; (3) the ash content should not exceed 10%, with a permissible variation of ± 1%; (4) the volatile matter should not exceed 1·5%; (5) the upper limit for the sulphur content should be mutually agreed upon between the consumer and supplier with an absolute maximum of 1·2%; (6) the as-received moisture content should not exceed 2%.—J. C. R.

Measuring the Air Blast Blown into the Cupola. H. Gernelle. (Fonderie, 1949, Apr., pp. 1554–1558). A description is given of the Pitot-Ritter apparatus for measuring air flow, with an account of its application for the measurement of the air blown into a cupola.—J. C. R.

Blast Humidity as a Factor in Cupola Operation. D. E. Krause and H. W. Lownie, jun. (American Foundrymen's Society, 1949, Preprint No. 11). Seventeen heats were made in a 10 in. dia. cupola to determine effects of moisture content of the blast upon operation of the cupola and the properties of the iron produced. The heats varied from 1 to 4 hr. in length and were made at various coke ratios and blast rates. The moisture content was controlled by Kathabar equipment at various levels from 27 to 219 grains of moisture per pound of air. Unusual precautions were taken in the operation and control of the cupola in order to obtain reproducible quantitative data on the effects of moisture in the blast. The amount of moisture in the blast had an appreciable effect upon the operation of the cupola and the properties of the iron produced. When the amount of moisture in the blast was increased, tapping temperatures and carbon absorption decreased significantly. The change in chemical composition that resulted from a change in the moisture content of the blast caused appreciable changes in the mechanical properties, chill depth, fluidity, and microstructure of the iron produced. Although changes in these properties were related to the change in chemical composition, there was no indication that the amount of moisture in the blast, in itself, directly affected the properties of the iron produced. Operation of the cupola and properties of the iron showed a rapid response to a change in the moisture content of the blast during a heat. Adjustments of the coke ratio and blast rate produced a partial compensation for the effects of high moisture in the blast, but generally each of the adjustments tested provided compensation that was insufficient to overcome completely the adverse effects of high moisture in the blast.

Production and Consumption of Heat in the Cupola. D. W. Gunther. (American Foundrymen's Society, 1949, Preprint No. 20). A number of theoretical calculations relating fuel quality to cupola performance are presented with the object of indicating maximum theoretical fuel efficiencies which can be obtained under various operating conditions. They are based on known and measured chemical and thermodynamic values and may be used to measure results obtained in practice. It is suggested that they may provide foundrymen with an indication of the most reasonable steps to be taken to improve cupola efficiency.—J. C. R.

Design and Operation of a 10-in. Diameter Cupola. D. E. Krause and H. W. Lownie, jun. (American Foundrymen's Society, 1949, Preprint No. 12). An account is given of operations with a 10-in. cupola designed to represent large commercial units as closely as possible except in size. Heats have ranged from 1 to 4 hr. at average melting rates of about 700 lb./hr. and average tapping temperatures were about 2780° F. Research investigations have been conducted on the effects of moisture in the blast, rusty scrap in the charge, relative refractoriness of various lining materials, and comparisons of the operating characteristics of different foundry cokes. Results have furnished dependable quantitative data applicable to large cupolas. A primary advantage of the small cupola is the ease with which operating variables can be controlled and recorded. Limitations of small cupolas must, however, be considered when applying results to larger cupolas. Possibilities of using small cupolas in research and development work, and commercial production of cast iron in small quantities are discussed. Constructional details and suggested modifications are given.—J. C. R.

Radioisotope Gage Indicates Liquid Metal Height in Cupolas. D. M. McCutcheon. (American Foundryman, 1949, vol. 15, June, pp. 35–39). An illustrated account is given of a radioactive isotope gauge, using cobalt 60, for indicating the level of liquid metal in a cupola. A unique design of the lead container for the radioisotope automatically provides radiation safety when the cupola is not in use. A pilot model is

described which meets the requirements of the production foundry.—J. C. R.

Controlled Atmosphere Annealing of Malleable Iron. (American Foundrymen's Society, 1949, Preprint No. 45). This report is compiled from replies to a questionnaire sent to malleable iron foundries in U.S.A. and Canada. Relatively few foundries did much about the control of atmosphere in their furnaces, but, in terms of tonnage of malleable iron produced, atmosphere control was practised to a marked extent. Gas containing carbon monoxide and carbon dioxide was used, but the ratio of the two varied considerably. This was not considered important in the first stage of the anneal. The presence of hydrogen or any gas that would produce free hydrogen including water vapour decreased the annealing rate.—J. C. R.

Deoxidation of White Cast Iron. R. W. Heine. (Foundry, 1949, vol. 77, Aug., pp. 74–78, 229–230; Sept., pp. 84–85, 239–243). A number of heats of white iron, melted in an induction furnace, were deoxidized with aluminium, 40% ferrotitanium, a special 40% zirconium alloy, or hydrogen. These treatments applied to white cast irons have been shown to affect some fundamental phenomena of the malleablizing process. The number of graphite nuclei developed during first-stage graphitization is markedly affected by the amount and kind of deoxidizing addition. The time required for first-stage graphitization is decreased by deoxidizing treatments when the element added functions primarily as a deoxidizer. Residual percentages of carbide stabilizing elements promote resistance to graphitization. It is concluded that oxygen in the forms in which it exists in white irons promotes resistence to graphitization. The practical applications of this theory are discussed in the second part of the article and the use of deoxidizers and the relative deoxidizing properties of various elements are described.—J. C. R.

Graphitization of Prequenched White Irons. P. Laurent and M. Ferry. (Société Française de Métallurgie, Oct. 21, 1948: Revue de Métallurgie, Mémoires, 1949, vol. 46, Oct., pp. 695–698). After summarizing the effects of a previous quenching treatment on the graphitization during the malleablizing of white cast iron, the authors describe their own work on the nucleation of graphite in cast iron containing carbon 2·5%, silicon 1·3%, manganese 0·35%, and traces of sulphur and phosphorus. The quenching treatment raised the number of graphite nodules from about 60 to several tens of thousands per sq. mm. The quenching temperature had no great influence on the number of nodules. The rate at which the temperature was raised to 950° C. during malleablization had little effect on unquenched irons, but in quenched irons with a slow rise of temperature the number of nodules exceeded 10,000–15,000 per sq. mm.

Prequenched samples were tempered at temperatures below the transformation point for various periods and then malleablized. The number of nodules reached a maximum at a certain temperature for each given tempering period; the maximum shifted towards lower temperatures for sufficiently long tempering times. Further, the variation of the number of nodules as a function of the holding time was much greater at low than at higher temperatures. The explanation suggested is that the martensite formed during quenching causes the precipitation of excess cementite in the α-iron lattice, which separates in particles that are finer the lower the temperature; if held long enough at temperature these cementite particles produce nodules of graphite. The rate of nucleation increases with the tempering temperature. For rapid graphitization a sorbitic structure is required, because nucleation takes place in the cementite of the sorbite (not in the eutectic cementite).—A. E. C.

Graphitization in the Malleable Process. H. G. Hall. (Fonderie, 1949, May, pp. 1571–1588). This is the full text, in French, of a paper presented to the October, 1948, meeting of the Association Technique de Fonderie, an abridged version of which appeared in Foundry Trade Journal, 1949, vol. 86, Jan. 20, pp. 55–57 (*see* Journ. I. and S.I., 1949, vol. 162, May, p. 114).

Some Effects of Deoxidation Treatments on Graphitization of White Cast Iron. R. W. Heine. (American Foundrymen's Society, 1949, Preprint No. 8). A study is presented of some effects produced when aluminium, titanium, and hydrogen are added to white cast irons. A study of deoxidation effects on inclusions revealed that eutectic sulphide inclusions were produced when the point of complete deoxidation was reached, as in the case of cast steels. By the effect on inclusions, chemical analysis for residual elements, and graphitization phenomena, the oxygen content of white cast iron is shown to be considerably higher than previously reported. The number of graphite nodules developed during first-stage graphitization is shown to decrease initially with small deoxidizing additions and then to increase rapidly as complete deoxidation is attained with the formation of a floccular rather than a nodular type graphite. The time required for first-stage graphitization is shown to be considerably reduced by deoxidation even with small additions. Sub-critical graphitization of the irons has been related to complete deoxidation. Indirectly, through inclusion studies and graphitization behaviour, the rôle of oxygen in malleablization is demonstrated.

Graphitizing Behavior of White Iron. S. C. Massari. (American Foundryman, 1949, vol. 15, May, pp. 67–72). A study was made of the graphitizing behaviour of a white iron, used in chilled car-wheel production, annealed at temperatures ranging from 1600° to 2000° F. so as to complete first-stage graphitization. Annealing was conducted under conditions which would practically eliminate initial graphitization while the metal was being heated from room temperature to the annealing temperature: a molten lead bath was used for this purpose. Experimental data established the approximate time required to complete first-stage graphitization and the type of graphite formed as a result of first-stage graphitization.—J. C. R.

Effect of Slag Types on Heat Treatment of Malleable Iron. G. Vennerholm and H. N. Bogart. (American Foundrymen's Society, 1949, Preprint No. 28: American Foundryman, 1949, vol. 15, Apr., pp. 96–97). A brief account is given of a method of slag control applicable in the electric-furnace melting of white iron for the production of malleable iron. Data are presented showing the relationship between the colour of the slag, its ferrous oxide content, and the soundness of the castings.—J. C. R.

Effects of Temperature and Silicon Content on First Stage Annealing of Black-Heart Malleable Iron. J. E. Rehder. (American Foundrymen's Society, 1949, Preprint No. 26). From an examination of published and unpublished experimental data on the relationship between silicon content and the time necessary for first-stage annealing of blackheart malleable iron, it is shown that plotting of the logarithm of the silicon content against the logarithm of the first-stage annealing time produces a straight line, the slope of which is dependent upon the carbon content of the iron. Furthermore, experimental data on the relationship between the temperature and the time necessary for first-stage annealing is shown to fall on straight parallel lines when plotted as logarithm of time against temperature. Application of the principles to commercial practice is discussed.

Influence of Silicon Content on Critical Temperature Range during Slow Cooling of Black-Heart Malleable Iron. J. E. Rehder. (American Foundrymen's Society, 1949, Preprint No. 27). A series of white cast-iron bars was made up from a pig iron and steel charge having carbon and silicon contents of about 2·55% and 0·76 to 1·57% respectively. After completion of first-stage annealing, the upper and lower limits of the critical temperature range during cooling at 7° F./hr. were determined. From the data obtained the following conclusions are drawn: (1) A critical temperature range exists for iron–carbon–silicon alloys in the range of chemical compositions employed in commercial malleable iron operations; (2) the upper and lower limits of this range, within which austenite, ferrite, and graphite co-exist in apparent equilibrium, rise and widen as silicon content increases; (3) in the range 0·75–1·60% of silicon and cooling rates 7–30° F./hr., pearlite appears, if at all, between 1360° and 1350° F.; (4) there is evidence that microsegregation of silicon in a dendritic pattern exists in malleable iron; (5) the solid solution existing within the critical temperature range, and appearing as troostite or martensite on quenching (depending on the silicon content) is believed to be austenite; and (6) cooling rate between first-stage annealing temperature and the upper limit of the critical temperature range does not affect the extent or progress of the reaction in the critical temperature range, at least at cooling rates as high as 600° F. hr.—J. C. R.

Influence of Rate of Heating on First Stage Graphitization of White Cast Iron. R. Schneidewind and D. J. Reese.

(American Foundrymen's Society, 1949, Preprint No. 37). Sections of step bars were annealed at 1700° F. It was found that the time necessary to complete first-stage graphitization decreased as the heating rate was decreased. A quantitative comparison was made with the earlier work of Boegehold and very satisfactory agreement was found. It seems that $\log \theta_R/\theta_\infty = -30/R$ where θ_R = hours at 1700° F. for first-stage annealing after bringing to heat infinitely fast, such as when a small sample is placed in a laboratory furnace already at 1700° F. Preheating at 800° F. showed some increase in annealing velocity although a proportional increase in time for heating to the soaking temperature seems better. Some theoretical discussion is presented regarding the mechanization of graphitization.

Pots for Annealing Malleable Iron. G. Joly. (Fonderie, 1949, May, pp. 1591–1592). The author briefly discusses the factors which influence the life of pots used for annealing malleable iron and suggests means for increasing the life of the pots.—J. C. R.

A Study of Insulating and Mildly Exothermic Antipiping Compounds Used for Steel Castings. S. L. Gertsman. (American Foundrymen's Society, 1949, Preprint No. 19). An account is given of a study of the more commonly used mildly exothermic and insulating type of antipiping materials. A standard mild-steel casting was used and test data were obtained on the thermal characteristics produced in the riser metal, the minimum size of riser that can be used for the standard casting with each compound, the type of shrinkage cavity produced in the riser, and the length of time required for the top of the head to freeze. The temperature gradient in the casting is reduced considerably when using these compounds. The method can be applied by foundrymen as a standard to evaluate the efficiency of any new compounds of a similar nature.—J. C. R.

Growth and Scaling Characteristics of High-Silicon Cast Irons. W. H. White and A. R. Alsea. (American Foundrymen's Society, 1949, Preprint No. 25). In this investigation on the growth of cast iron an attempt has been made (1) to obtain a better understanding of growth mechanism, (2) to develop a superior growth-resistant cast iron that could be produced and cast in the average grey-iron foundry, (3) to emphasize the advantages of using high-silicon cast iron for applications requiring growth resistance, and (4) to increase the ductility of the inherently brittle high-silicon cast iron by making small alloy additions. Data are presented derived from growth, scaling, and mechanical tests on specimens of high-silicon and ordinary cast iron, both alloyed and unalloyed. Results confirmed the theory that growth is the result of the metal yielding to stress set up by irregular expansion and contraction on heating and cooling through the critical temperature range. Oxidizing gases greatly accelerated growth, small voids and cracks formed in passing through the critical temperature range becoming filled with oxides and affording a barrier to any compressive forces that might be encountered on subsequent heating and cooling. A small but definite volume increase was caused by decomposition of carbides. High-silicon (6%) cast irons resist growth at temperatures as high as 1650° to 1700° F. since they are free from carbides and their lower critical temperature is about 1775° F. The type and distribution of graphite flakes in high-silicon cast irons determined their resistance to scaling. Alloy additions failed to reduce brittleness in high-silicon irons and unalloyed silicon irons had better room-temperature mechanical properties than alloyed irons.—J. C. R.

High-Duty Silicon Iron. R. V. Riley. (Chemical Age, 1949, vol. 61, July 2, pp. 21–23: Metallurgia, 1949, vol. 40, Aug., pp. 185–188: Foundry Trade Journal, 1949, vol. 87, Aug. 18, pp. 221–224). The technique for casting a high-silicon iron heater tube (5 ft. long, 7 in. in outside dia., $\frac{3}{4}$ in. thick, with one closed end) for a sulphuric acid concentrating plant, is described. This tube in service is totally immersed in dilute sulphuric acid and carries live steam at 100 lb./sq. in. The physical and mechanical properties of Hypersilid alloy, of which the tube was made, are given.—R. A. R.

Steel Casting of Locomotive Components. (Railway Gazette, 1949, vol. 91, Nov. 11, pp. 560–562). A description is given of the founding technique for the production of cast-steel locomotive wheel centres up to 6 ft. 6 in. in dia. A split wood pattern is packed with good quality facing 'compo' and the space between the spokes is packed with a weaker mixture of sawdust and sand. Steel is provided from one or two converters which are in turn supplied from a cupola.—R. A. R.

Casting a Long Roll Grinder Bed. P. Dwyer. (Foundry, 1949, vol. 77, Sept., pp. 86–88, 220). The author gives a detailed illustrated account of the casting of a roll grinder bed, 42 ft. 9 in. long, free from warpage and shrinkage, and having a cleaned weight of 58,020 lb.—J. C. R.

An Unusual Steel Casting. (British Steelmaker, 1949, vol. 15, Aug., pp. 377–379). A brief account is given of the moulding and pouring techniques adopted for casting in a single piece the trolley frame for a 10-ton charging machine for open-hearth furnaces. Both mould and cores were made of 'compo' instead of sand. Using two 45-ton double-nozzle bottom-pouring ladles, the metal was poured simultaneously at four outside corners, each point pouring 4 tons/min. Twenty-eight tons were actually required, and a small alloy addition was made to increase the fluidity of the metal.

Gray Iron Parts Successfully Surface-Hardened in the Flask. A. P. Alexander. (Materials and Methods, 1949, vol. 30, Aug., pp. 58–59). An economical and practical method of making small grey cast-iron check pieces (weighing about 5 lb.) with one face hardened to a depth of $\frac{5}{32}$ in. was to use a chill in the mould and to place about 5 g. of tellurium in the runner supplying two castings.—R. A. R.

Ship's Castings. Modern Methods and Equipment for the Production of Large Units. (Iron and Steel, 1949, vol. 22, Aug., pp. 383–384). An account is given of some of the foundry problems involved in the casting of large parts for ship's machinery and equipment.—J. C. R.

Cast Iron Crankshaft Manufacture. (Gas and Oil Power, 1949, vol. 44, Aug., p. 199: [Abstract] Journal of The British Shipbuilding Research Association, 1949, vol. 4, Oct., p. 465). An American company manufactures special cast-iron oil-engine crankshafts weighing from 500 to 5000 lb., and the article gives an account of composition, properties, and casting process. The cast-iron specifications vary with the dimensions of the shafts; but the carbon content is limited to 2·2–3·0%. The main alloying constituents are nickel (1–2·5%) and molybdenum (0·85–1·2%); and the matrix of the microstructure is largely acicular with random distribution of fine flake graphite. It is stated that the shafts meet specifications calling for strengths of 60,000 to 75,000 lb./sq. in. Crankshafts less than 85 in. long are machine moulded from patterns on a steel board, longer ones (up to 21 ft.) on rigid wooden boards with a stationary sand slinger. All cranks are poured at an angle of about 7° from the horizontal through a single gate. On completion of the pouring operation the moulds are brought into the vertical position and so cooled, thus allowing the riser to feed the casting by gravity. The cranks are taken out at a temperature between 300° and 500° F. They are cleaned by the usual methods and the excess sand is removed with pneumatic hammers. Risers are removed by sawing. The crankshafts are then stress-relieved by thermal treatment at 700° to 800° F. for not less than 8 hr., followed by slow cooling in the furnace. They are shot-blasted subsequently, and inspected for surface defects and straightness.

Grinding Balls. E. F. Farren. (Foundry Trade Journal, 1949, vol. 87, Aug. 4, pp. 155–157). A description is given of the procedure adopted for the production by centrifugal casting of 3-in. dia. grinding balls of W.B. Grade Meehanite (instead of manganese steel). The balls were cast in nests and were easily separated from the runner and very little fettling was required.—J. C. R.

Prevention of Hot Tears in Thick-Walled Centrifugally Cast Steel Tubes. J. F. Wallace and J. L. Martin. (American Foundrymen's Society, 1949, Preprint No. 3). Longitudinal and spiral hot tears in centrifugally cast tubes are believed to be caused by the internal pressure of the molten steel under centrifugal force acting against the solidified outer shell of steel. The different variables which have to be considered in evolving a satisfactory centrifugal-casting technique are discussed and mathematical expressions for these have been developed. From these a single equation can be applied to compute the maximum rate of pour possible without hot tearing at any time during the casting cycle for a given size tube and speed of rotation.—J. C. R.

Machine Molding of Steel Castings. J. H. Hall. (Foundry, 1949, vol. 77, July, pp. 78–81, 186–191; Aug., pp. 82–85, 175–181; Sept., pp. 76–77, 166–179; Oct., pp. 90–91, 174–180). This series of four articles describes machine moulding practice in the steel foundry. The first two parts deal with sand-ramming machines and snap and strip flask moulds.

Part three discusses the use of roll-over and jolt-squeeze moulding machines, and part four, the application of sand-slingers and core blowers.—J. C. R.

Technical Notes on Cylinder Block Production. H. H. Shepherd. (Australian Institute of Foundrymen : Australasian Engineer, 1949, July 7, pp. 67–74 ; Aug. 7, pp. 51–58). A comprehensive account is given of the foundry problems of cylinder-block production, Part I dealing with core and mould preparation, and Part II with melting practice, metal mixtures, and control.—R. A. R.

Microstructural Diagrams for Cast Iron. E. Schmitt. (Neue Giesserei, 1949, vol. 36, July, pp. 207–209). The suitability of various diagrams in the literature for aiding the foundryman to obtain the desired microstructure in iron castings is considered. The iron-carbon, Maurer, and Greiner–Klingenstein diagrams are rejected for this purpose, whilst K. Sipp's diagram (*see* Journ. I. and S.I., 1941, No. II, p. 31A) is approved. This diagram, with degree of carbon saturation plotted against specimen thickness, is modified by the author to take into account seven types of mould (water-cooled chill, cold and preheated chills, oil sand, green sand, and cold and preheated dry sand). An example of the practical application of this diagram is given.—R. A. R.

Comparative Solidification Studies. V. Paschkis. (American Foundrymen's Society, 1949, Reprint No. 42). Solidification rates of various steel shapes cast in sand were compared. Ratios of solidification times for the different shapes are not significantly changed by change of conductivity or specific heat of the steel. Data are presented graphically. The ratios of solidification time of a slab to that of a cylinder and of solidification time of a slab to that of a sphere, respectively, are plotted against relative positions (surface, half-thickness, centre, etc.). Curves are given for five degrees of superheat.

Use of Tellurium in Promoting Chills on Gray Iron Castings. C. R. Austin. (Foundry, 1949, vol. 77, July, pp. 74–77, 228, 230, 233, 234). The advantages of adding tellurium (0·1 to 0·005% or less according to the nature of the iron and the chill required) are outlined. Tellurium can be added as copper–tellurium pellets or as a powder in a copper tube to the ladle, or used in a core wash or sand impregnation. The function of the tellurium is to reduce the critical cooling rate of the iron. Illustrated descriptions are given of fractures of castings to which chill has been imparted by the addition of tellurium, and of its use in a core wash. Particular reference is made to its application in the manufacture of wheels and gears.—J. C. R.

The Foundryman and Heat Transfer. V. Paschkis. (American Foundrymen's Society, 1949, Preprint No. 43). This paper is divided into three parts. In the first the elements of heat flow are explained, the second deals with thermal problems in the foundry, and the third with the electric analogy method of heat transfer. This method utilizes the fact that heat flow in solid bodies follows the same mathematical laws as the electric current in a non-inductive conductor with evenly distributed capacitance.—J. C. R.

Friction Saw Cuts Castings Cleaning Cost. H. J. Chamberland. (American Foundryman, 1949, vol. 15, June, pp. 44–47). An account is given of friction sawing for the cleaning of castings, for which heavy and uninterrupted work-feeding pressure is essential. A material thickness of 1 in. is considered to be the maximum for friction sawing, although sections up to 1½ in. may be cut if the casting can be rocked to present a continually thinning section to the saw. Hardness has no effect but alloying constituents have a considerable bearing on the saw speed needed to create the most frictional heat in the least possible time.—J. C. R.

Mechanized Units Installed by Textile Machine Works Foundry. W. G. Gude. (Foundry, 1949, vol. 77, July, pp. 82–87). An illustrated description is given of the foundry of the Textile Machine Works, Reading, Pa., with particular reference to a new highly mechanized unit for the large-scale production of small and medium size grey-iron castings.

J. C. R.

6 Ways to Mechanize a Foundry. H. W. Zimnawoda. (American Foundryman, 1949, vol. 15, Apr., pp. 79–90). Plans, illustrations, and detailed descriptions are given of six different but typical layouts for a mechanized foundry.

J. C. R.

Foundry Data—Collecting, Organizing, Analyzing. W. K. Bock. (American Foundryman, 1949, vol. 15, June, pp. 53–56). The author discusses the application of statistics to foundry data. In particular he deals with the reproducibility of mechanical properties, considering (1) the collection of data ; (2) the organization of data ; and (3) the analysis of data.—J. C. R.

Foundry Dust. J. Gardner. (Iron and Steel, 1949, vol. 22, Aug., pp. 385–388). A survey of the work on silicosis prevention is presented, and some indications of the directions in which dust control in the foundry is being effected, are given.

HEATING FURNACES AND SOAKING PITS

Comparative Advantages of Modern Soaking Pits. E. A. Brown, jun. (Steel, 1949, vol. 125, Aug. 15, pp. 94, 96, 127, 128, 130, 132). Modern soaking pits are usually of recuperative type and may be 'top one-way,' 'top two-way,' 'bottom two-way,' vertically, or tangentially fired. These designs all enable ingots to be uniformly heated, controlled to correct temperature, and free from excessive scale : fuel consumption is lower than on older regenerative types, production rate for a given area is higher, and bottom maintenance is reduced.

J. P. S.

Fundamental Data for Induction and Dielectric Heating. (Industrial Heating, 1949, vol. 16, June, pp. 990–992, 994 ; July, pp. 1188–1194 ; Aug., pp. 1374–1376). In the first part of this article the advantages of induction and dielectric heating are set out. The second part presents formulæ for calculating the quantities connected with radio-frequency heating, induction heating, and dielectric heating.—R. A. R.

The Transmission of Heat in Metallurgical Furnaces. Bibliography. C. Hulse and R. J. Sarjant. (Revue de Métallurgie, Mémoires, 1949, vol. 46, Nov., p. 774). This bibliography of some 40 references was omitted when the text of the paper was printed in *Revue de Métallurgie, Mémoires*. 1949, vol. 46, May, pp. 297–308 (*see* Journ. I. and S.I., 1950, vol, 164, Jan., p. 107).

HEAT-TREATMENT AND HEAT-TREATMENT FURNACES

The Heat-Treatment of Steel. W. Hiller. (Österreichischer Maschinenmarkt und Elektrowirtschaft, 1949, vol. 4, Sept. 15, pp. 314–319). The principles of the heat-treatment of steel are explained by reference to the iron–carbon diagram, the microstructures obtainable are described and illustrated, and it is shown how to use **S**-curves to predict the structure which will be obtained after a given cooling rate.—R. A. R.

Hot Die Steels, Their Properties and Treatment. K. G. Morrison and D. Cornwall. (Australian Institute of Metals : Australasian Engineer, 1949, July 7, pp. 52–59). The properties, heat-treatment, and applications of drop forge die block steels and hot-working steels are discussed. In drop forging, the hot metal is in contact with the die for only a short time, and the average temperature does not as a rule rise above 150–200° C. Seven recommended analyses are given ; one in common use in Australia for very severe conditions is: C 0·60%, Mn 0·65%, Ni 2·00%, Cr 0·70%, Mo 0·75%. The hot-working steels are classified according to the presence of the elements chromium, tungsten, and chromium plus tungsten. The heat-treatments and applications of these are described and 22 analyses are given. A simple method of removing scale is mentioned ; the heated steel forging is dipped for a moment into finely powdered salt and immediately passed rapidly through water at close to boiling point.—R. A. R.

The Basic Principles of Carburizing. E. Barber. (British Steelmaker, 1949, vol. 15, Mar., pp. 130–133 ; Apr., pp. 182–185 ; June, pp. 282–285 ; Aug., pp. 390–393 ; Sept., pp. 444–447). This is a comprehensive account of the theory and practice of case-carburizing which includes descriptions of the equipment, subsequent heat-treatments, and methods of keeping local areas soft, and gives the compositions of steels suitable for carburizing.—R. A. R.

Case-Hardening Tests with Mildly Effective Powders. A. Pomp and A. Krisch. (Mitteilungen aus dem Kaiser-Wilhelm Institut für Eisenforschung, 1944, vol. 27, No. 10. pp. 139–152). A 0·14% carbon steel and six low-alloy chromium steels, some containing nickel and/or molybdenum, were pack-carburized using two powders prepared from brown coal. Increasing the carburizing time 300% caused a greater increase in carbon absorption than an increase of 40° C. in the temperature. The hardness of the surface layer was affected in the same manner. The microstructures of the case and transition zone were also examined.

Developments in Nitrogen Case Hardening. S. C. Churchill. (Mechanical World, 1949, vol. 126, July 1, pp. 5–8). The effect of alloying elements on the hardness and case depth produced by nitriding is discussed. Carbon–chromium and carbon–chromium–molybdenum steels nitride to give a maximum hardness of 800–850 Brinell, which diminishes gradually towards the core. Carbon–aluminium with or without molybdenum gives a nitrided hardness of 1000 Vickers, and when chromium is added the case is one of the hardest known in ferrous metallurgy, reaching as high as 1200 Vickers. Molybdenum greatly improves the diffusion of nitrogen in carbon–aluminium steels and reduces the embrittling effect. Polished nitrided steels offer high resistence to fatigue and corrosion-fatigue conditions. Steels alloyed for subsequent nitriding are weldable, but require the same precautions against cracking as employed for high-strength alloy steels; best results are obtained with the atomic hydrogen process. R. A. R.

A Critical Review of the Nitriding Process. L. F. Spencer. (Steel Processing, 1949, vol. 35, June, pp. 311–318; July, pp. 375–380, 382, 390). The compositions of nitriding steels are first described, with the relative effects of various elements on the hardness and depth of case; next, the process of nitriding itself is described with details of machinery and heat-treatment, and recommended compositions for the work-containers and the nitriding muffle or retort. Nitrided structures are shown in micrographs and grain growth is discussed. The austenitic stainless steels can be satisfactorily nitrided, but the ferritic and high-nickel stainless steels do not respond well.—R. A. R.

Flame Hardening Large Gears. P. A. Furkert. (American Gas Association: Industrial Heating, 1949, vol. 16, July, pp. 1172–1174). A description is given of a gas-fired heat-treating unit for hardening the teeth of gear wheels about 28 in. in dia. × 5 in. face, weighing about 400 lb. It comprises a stand on which the gear is revolved while surrounding it is a ring of 24 burners positioned radially so that the flames impinge on the teeth.—R. A. R.

Versatility of Oxyacetylene Flame-Hardening. J. T. Howat. (Steel, 1949, vol. 125, Aug. 8, pp. 64–67, 94). The oxy-acetylene flame is very near to an ideal source of heat for flame-hardening. This article describes a number of applications of the process, in the hardening of steels, alloy cast irons and pearlitic malleable irons.—J. P. S.

The Effectiveness of Low Temperature Stress Relief as Applied to Welded Ships. U. W. Hird. (Society of Naval Architects and Marine Engineers: [Abstract] Journal of The British Shipbuilding Research Association, 1949, vol. 4, Aug., pp. 352–353). The Linde Air Products Company and the Sun Shipbuilding and Dry Dock Company have collaborated in an extensive investigation to determine the effectiveness of the low-temperature stress-relieving process and the possibility of applying it to the critical welds in ships. The commercial application of the process is described in detail. The process was proved to reduce residual stresses in welded joints to negligible amounts. It can be applied to the critical welds in the central two-thirds of the length of a ship either during or after construction with almost equal effectiveness. It has been accepted as a simple and inexpensive method of relieving the residual stresses in the accessible welded joints of structures too large to be stress-relieved in a furnace.

Cold-Treatment of Steel. (Österreichischer Maschinenmarkt und Elektrowirtschaft, 1949, vol. 4, July 31, pp. 260–261). When molybdenum and tungsten high-speed steels are quenched to about 215° C. about 80% of the austenite is transformed to martensite. On quenching from 1205° to −84° C. and tempering at 560° C. for 1 hr. and repeating this treatment, the residual austenite is reduced to 8–10% after two treatments, and to 5% after three.—R. A. R.

The Application of Electric Heat to Iron Production, Processing, and Refining. G. Keller. (Brown Boveri Review, 1949, vol. 36, May–June, pp. 191–201). Brief illustrated descriptions are given of electric furnace installations for making pig iron and steel, reheating rods, annealing tubes, patenting wire, heat-treating parts (including watch springs), and stoving enamelled pans.—R. A. R.

FORGING, STAMPING, DRAWING, AND PRESSING

Power and Drop Hammers. (Overseas Engineer, 1949, vol. 23, Aug., pp. 15–19). Steam, air, and mechanically operated hammers of recent design are described and illustrated.—R. A. R.

Die Sinking for Drop Forging. J. Mueller. (Steel Processing, 1949, vol. 35, Feb., pp. 75–78; Apr., pp. 196–199; June, pp. 301–305; Aug., pp. 415–417, 444). The making of board hammer drop forging dies is described with data on the steel compositions, hardness, and recommended dimensional limits. The machine and hand tools employed, and the materials-handling system in the machine shops are described and illustrated.—R. A. R.

When to Use Steel Drop Forgings for Stressed Parts. K. Rose. (Materials and Methods, 1949, vol. 30, Aug., pp. 63–65). The advantages and limitations of steel drop forgings are discussed and compared with those of castings. Two examples of successful drop forgings described are: (1) An axle box for heavy railway wagons which was made of two drop forgings welded together; and (2) a clutch yoke.—R. A. R.

Cast Forging Dies. H. Kaessberg. (Archiv für Metallkunde, 1949, vol. 3, June, pp. 225–226). Recommendations on the composition and heat-treatment of cast steel forging dies are made, and their advantages discussed.—R. A. R.

Trimming Tools. P. Patek. (Betrieb und Fertigung, 1949, vol. 3, July, pp. 97–105). Recommendations are made on the design of trimming tools for removing the flash from forgings. R. A. R.

Precision Forging for Gas Turbine Blades. C. H. Smith. (Machinery, 1949, vol. 75, Oct. 13, pp. 511–516). The author describes the technique employed at the Steel Improvement and Forge Co., Cleveland, U.S.A., for the precision forging of turbine blades. Dies for precision forging consist of inserts made of hot-work tool steel (C 0·35%, Mn 0·3%, Si 1%, Cr 5%, V 0·4%, W 1·25%, Mo 1·5%) shrunk or keyed into bodies of standard die material. The blades are forged to very close tolerances, *i.e.*, the thickness is maintained within ± 0·005 in., warp or twist tolerance is held within ± 0·5°, variation from true blade contour to ± 0·003 in., and deflection in the overall length of blade to ± 0·01 in.—R. F. F.

Calculations and Tests on the Cold Pressing of Shells. M. Dipper. (Archiv für das Eisenhüttenwesen, 1949, vol. 20, Sept.–Oct., pp. 275–286). Calculations and tests are reported the object of which was to study the flow of material when cold pressing cylindrical steel shells and to derive formulæ for predicting the power requirements for pressing shells of different wall thicknesses and lengths. Cylinders 25 mm. in dia. of aluminium, two aluminium alloys, and lead were pressed into shells, the operation being interrupted at various stages and the flow of material studied. The data supported the theory that the process was one of double extrusion. R. A. R.

Practical Problems of Light Presswork Production. J. A. Grainger. (Sheet Metal Industries, 1948, vol. 25, Jan., pp. 99–102; Mar., pp. 511–516; Apr., pp. 719–722; June, pp. 1145–1152; July, pp. 1347–1352, 1354; Aug., pp. 1561–1568; Sept., pp. 1771–1775; Nov., pp. 2201–2205; 1949, vol. 26, Jan., pp. 98–102; Mar., pp. 531–534, 539; June, pp. 1227–1234; Aug., pp. 1703–1713, 1720). Conclusion of a series of articles (*see* Journ. I. and S.I., 1948, vol. 158, Apr., p. 533).

New Production Methods for Transformer Sheets. Y. S. Krongauz and N. I. Nazarevsky (Vestnik Elektropromyshlennosti, 1948, No. 11, pp. 24–28: [Abridged Translation]. Engineers' Digest, 1949, vol. 10, Sept., pp. 325–326). The new methods for the production of power transformer sheets at the Kuybyshev Transformer Factory in Moscow are described. Production is organized in four lines with straight-through flow. Stamping the sheets to size and punching the holes have been combined into one operation, and fully automatic grinding and conveyor belt transport between operations have been introduced. Labour requirements have been reduced and output increased by 97%.—R.A.R.

The Efficiency, Capacity, and Determination of the Efficiency of Stamping Shops. H. L. Hilbert. (Die Technik, 1949, vol. 4, June, pp. 265, 268). Methods of determining the possible output and the efficiency of production in stamping shops are explained and compared.—R. A. R.

The Calculation of Tools for Drawing Non-Cylindrical Hollow Bodies. B. Eisler. (Österreichischer Maschinenmarkt und Elektrowirtschaft, 1949, vol. 4, Aug., 31, pp. 284–287). Formulæ are developed for the design of punches and dies for pressing hollow bodies with walls of changing diameter.

Some Theoretical Aspects of the Determination of Plastic Strains in Sheet Metal. K. L. Jackson. (Sheet Metal Industries, 1949, vol. 26, July, pp. 1447–1451; Aug., pp. 1715–1720). The paper, which is of a mathematical nature, is concerned with the calculation of drawing strains in the single-stage drawing of circular shells of 0·010-in. tinplate.—R. A. R.

Phosphatizing as an Aid to the Plastic Deformation of Iron and Steel. A. Durer. (Archiv für des Eisenhüttenwesen, 1949, vol. 20, Sept.–Oct., pp. 305–311). The physical and chemical properties of phosphate coatings and the reasons for the assistance they give in drawing operations are discussed. The phosphate coating used in drawing and pressing consists of the zinc phosphate $Zn_3(PO_4)_2.4H_2O$ of the rhombic modification. A little iron may replace some of the zinc without changing the modification. The layer is from 0·5 to 20μ thick and consists of rather brittle crystals with a microhardness less than that of 0·1% carbon steel. In the deformation process part of the phosphate coating is rubbed off; the remaining layer has increased adherence and a bright surface. The surface does not behave as a lubricant; its excellent bearing properties are, on the contrary, due to the physical change in the nature of surface mentioned above. The surface capillarity of the phosphate layer plays a less important part, and the surface is wetted much more by hydrocarbons and similar substances than by metals. Soap solutions react with part of the surface to form zinc soaps which are precipitated and form an effective lubricating film. Friction reaches a minimum at very small coating thicknesses, and with increasing thickness the friction gradually rises to a very considerable value. The optimum coating thickness is that at which the friction is not too great, while it remains sufficient to offer good wear resistance; its value is different for each particular case, but in general, heavy deformation requires a greater thickness.

A good adherent coating depends mainly on careful cleaning of the surface before phosphatizing; its thickness and structure can be controlled by adjustment of the process conditions. Carbon steels and low-alloy and manganese steels can be phosphatized, but high-alloy steels, with two or more carbide-forming elements, and steels alloyed with aluminium, and mechanically prepared surfaces (*e.g.*, sand-blasted) are unsuitable.—R. A. R.

Cold Shaping of Steel. (Steel, 1949, vol. 125, July 25, pp. 58–60, 94, 96, 99; Aug. 1, pp. 74–77). The Heintz Manufacturing Co., Philadelphia, has been carrying out work on Government contract, on the cold extrusion of steel, first developed in Germany during the war. This article describes the theory of extrusion, both 'backward' and 'forward,' and its application to low-carbon steel of 'mill run,' as against the non-ageing, aluminium-killed steels treated by the process in Germany. Preliminary heat-treatment should include annealing, and for extreme reductions both normalizing and annealing; where multiple-extrusion operations are carried out intermediate annealing is also necessary. The design and material of dies requires special care, and lubrication is of considerable importance; phosphate coatings are very valuable, and their thickness should be proportional to the degree of cold work. The frictional resistance of this will decrease as it reaches the amorphous and transparent state due to pressure, and the use of lubricants such as sulphonated tallow also reduces friction. A degreasing-phosphatizing-lubricating line is described.—J. P. S.

Sintered Carbide Die Construction. (Industrial Diamond Review, 1949, vol. 9, Aug., pp. 232–233). The production of a sintered carbide die for the first draw in a deep-drawing operation is described with special reference to fitting the die ring to its support, and to the boring, grinding, and lapping of the ring.—R. A. R.

Methods of Facilitating the Drawing of High-Alloy Steels. F. Rossteutscher. (Archiv für Metallkunde, 1949, vol. 3. Aug., pp. 282–283). The phosphatizing of carbon steel to carry a lubricant and facilitate drawing operations cannot be applied to high-alloy steels as the alloying elements. nickel, chromium, copper, manganese, cobalt, and silicon. when more than 6% is present, make phosphatizing difficult. A recent I.G. Farbenindustrie patented process is briefly described by which these alloy steels can be treated with oxalic acid containing soluble sulphide or hydrogen sulphide. This treatment will cause a greenish adherent uniform coating to form on the surface, even on alloys which are inert to oxalic acid alone. The coating which, on chromium–nickel steels probably consists of an intermediate layer of nickel sulphide and chromium hydroxide with adsorbed iron sulphide, reduces the power and lubricant requirements in subsequent drawing operations.—R. A. R.

Continuous Rod Drawing, Straightening. Polishing Performed at High-Speed on Both Ferrous and Non Ferrous Stock. D. Reebel. (Steel, 1949, vol. 125, July 11, pp. 100–101, 104). A unit is described for the continuous drawing, straightening, cutting to length, and polishing of steel, brass, copper, and other non-ferrous rods of round, hexagonal, square, or rectangular section. These are received in coils and pass through an uncoiler, a roller straightener which also applies back-pull during the drawing, a continuous draw-bench where two carriages operate in turn, two further sets of straightening rolls, and a flying shear. The final portion of the unit straightens, polishes, and stress-relieves the cut lengths, passing them between motor-driven discs.—J. P. S.

Stresses and Strains in Tube-Drawing. H. W. Swift. (Philosophical Magazine, 1949, vol. 40, Sept., pp. 883–902). An attempt is made to examine in some detail the problem of 'empty sinking,' a tube, *i.e.*, drawing without a restrictive mandrel, in order to test the validity of certain simplifying assumptions, to ascertain the probable effects of various impressed conditions (*e.g.*, strain-hardening), and to compare the results with such revelant experimental data as are available. Two modes of sinking are analysed: one in which the motive force is transmitted as tension in the drawn tube, and the other in which the tube is pressed through.—R. A. R.

Die Polishing. (Wire Industry, 1949, vol. 16, June, p. 487). The importance of a high polish on carbide wire-drawing dies is stressed. A recent innovation is a comprehensive range of diamond-oil compounds, made up in cartridge form, covering a series of diamond particle size and concentration. The compound is applied to the work in exactly metered amounts by a gun.—R. A. R.

Application of VSG Units to Wire Drawing. (Machinist, 1949, vol. 93, Aug. 27, pp. 637–638). *See* Journ. I. and S. I., 1949, vol. 163, Dec., p. 465.

Electrical Controls for Wire Mill Machinery. O. M. Bundy. (Wire and Wire Products, 1949, vol. 24, July, pp. 595–598, 617–622). Recommendations are made on the control of motors driving wire-drawing machines.—R. A. R.

ROLLING-MILL PRACTICE

The Cogging Mill in the Special-Steel Rolling Mill. E. A. Spenlé. (Stahl und Eisen, 1949, vol. 69, June 23, pp. 443–450). The author considers the factors to be taken into account when determining the lowest throughput at which the operation of a cogging mill for special steels still pays. A two-high reversing mill is better than a three-high mill. The reversing mill should be heavy enough to reduce, with several reheatings, ingots which are difficult to deform, as this is more economical than forging. The minimum economic unit is one with rolls about 840 mm. in dia. which should have a production of about 12,500 tons of blooms per month (*see* Journ. I. and S.I., 1950, vol. 164, p. 226, Translation Series, No. 393).—R. A. R.

A Fast Rod Mill. (Engineer, 1949, vol. 188, Nov. 18, p. 591). A rod mill made by Davy and United Engineering Co., Ltd., for Guest, Keen and Nettlefolds, Ltd., in South Wales, is very briefly described. It has 23 stands and will produce all sizes of rods from 5 gauge at 25 tons/hr. up to $\frac{3}{8}$ in. in dia., as well as coiled bars up to 1 in. in dia.—R. A. R.

Keystone's New Rod Mill. (Wire and Wire Products, 1949, vol. 24, Sept., pp. 775, 787, 788). Brief details of the installation of a new 19-stand rod mill at the works of Keystone Steel and Wire Co., Peoria, Illinois, are given. This mill reduces $2\frac{1}{2}$ in. square billets to slightly below $\frac{1}{4}$ in. in dia., the finishing speed being 4500 ft./min.—R. A. R.

Production of Structural Shapes and Rails. F. M. Gillies and W. E. Dittrich. (Steel, 1949, vol. 125, Aug. 22, pp. 82–88, 93–96; Aug. 29, pp. 74–78, 81–82). In the first part of this article the authors describe typical mill practice for the rolling of sections, covering the whole process from the blooming mill to the cut-off shears, and giving examples of types of mill, and of reheating furnace employed. In the second part, the rolling of rails is considered, pass schedules illustrated, and straightening devices described.—J. P. S.

The Hot Rolling of Wide Strip. Sedlaczek. (Die Technik, 1949, vol. 4, Sept., pp. 432–435). The advantages and disadvantages of continuous wide strip mills and the Steckel

tension mill with a four-high stand between two furnaces containing coilers are discussed. The Steckel mill at Maxhütte, Unterwellenborn is described.—R. A. R.

Heating and Cooling Equipment for the Rolls of Sheet Mills. W. Krämer. (Stahl und Eisen, 1949, vol. 69, Sept. 29, pp. 711–712). Adjustable gas burners, and water and air-cooling equipment for the rolls of sheet mills are described. R. A. R.

Carnegie-Illinois Modernizes Cold-Reduced Sheet and Tin Plate Production at Gary. (Industrial Heating, 1949, vol. 16, July, pp. 1204–1212). *See* Journ. I. and S.I., 1949, vol. 162, Aug., p. 484.

Sheet, Strip and Tinplate Industry in Western Europe. P. Damiron. (Iron and Steel Engineer, 1949, vol. 26, Aug., pp. 75–81). A brief survey is presented of the present position of the sheet, strip, and tinplate industries of Great Britain, France, Holland, and Luxembourg.—J. C. R.

Production of Hot and Cold Rolled Strip and Sheets. C. L. McGranahan. (Steel, 1949, vol. 124, June 6, pp. 106, 117; June, 13, pp. 88–101; June 20, pp. 106–117; June 27, pp. 66–76; July 4, pp. 83–84, 90–95; July 11, pp. 106–116; July 18, pp. 91–101; July 25, pp. 80–90). In the first part of this particle, the author describes at some length the history of sheet rolling, from 'pack' methods to the continuous mill, and begins a description of the production of hot-rolled sheet and strip on a modern continous mill; in the second, he traces the whole process from the slab-heating furnace through scale breaker, roughing stand, finishing stand, run-out tables, coilers, and pilers, and includes a brief description of the Steckel mill. In the third part, he describes the recognised commercial qualities of sheet and the methods of obtaining them, and explains the processes of batch and continuous pickling. The fourth part enumerates the qualities of cold-rolled steel, and describes the cold-rolling process, with reference to conventional mills, reversing mills and the Steckel mill. In the fifth part, the construction and driving of continuous mills is described together with the annealing of both hot- and cold-rolled sheet. The sixth part describes the function of the temper mill both on strip and on sheets, and passes on to the special problems of the manufacture of sheets for vitreous enamelling. The properties to be associated with various tempers and surface finishes are also outlined. The seventh part deals with hot-dip and electrolytic galvanizing, a continuous hot-dip line being described; in the eighth and last part, a continuous electrolytic wide-strip line is described.

Included in the last part is also a brief description of the cold-reduction of stainless-steel sheet.—J. P. S.

Ingot to Coiled Hot Strip in Six Minutes. D. Reebel. (Steel, 1949, vol. 125, July 25, pp. 66–70). The Newport Steel Corp., Newport, Ky., has recently installed a hot strip plant of 60,000 tons/month capacity. The ingots as received are reheated in one of five circular heating pits, fired either by oil or natural gas; they then pass to a 54-in. reversing mill, and to a 54-in. two-high reversing breakdown mill. After nine passes through the edger and 15 through the breakdown mill, which takes 3 min., an ingot 18 in. by 39 in. by 72 in. is reduced to 0·625 in. thick. After cutting to length in a shear, the strips pass into an oscillating roller-hearth furnace 154 ft. long. The rollers rotate to and fro six times a minute to prevent them sagging under the strips. Next comes a four-high hot reversing mill, where the strips are coiled on reels in furnaces between passes through the rolls, five passes of which are necessary to reduce the thickness from 0·625 in. to 0·140 in.—J. P. S.

Mandrel Type Hot Strip Coiler. G. E. Farrington. (Iron and Steel Engineer, 1949, vol. 26, July, pp. 69–73). An illustrated and detailed description is given of a mandrel type hot strip coiler and its method of operation. It consists essentially of a pair of deflector pinch rolls of unequal diameter placed on the table pass line just ahead of the gate to the coilers. A throat, or enclosed guide arrangement topped by the gate, leads the strip to the cluster of blocking rolls and mandrel. Four sets of motor-driven blocking rolls surround a collapsible motor-driven mandrel. The four blocking roll carriages slide in straight lines radially away from the mandrel. All four sets are linked together and are operated by two air cylinders. The mandrel is similar to a cold mill tension reel except that it always forms a circle whether expanded or collapsed, and has a greater diametrical difference between expanded and collapsed positions.—J. C. R.

Some Fundamental Questions Concerning Cold Rolling Deep-Drawing-Quality Wide Strip. O. Emicke. (Die Technik, 1949, vol. 4, Sept., pp. 435–440). The dimensions of product and coil weights of wide strip mills of different types are reviewed and the type of hot mill with cold finishing to produce deep-drawing quality wide strip for German conditions is discussed. The mill recommended should produce 1½-ton coils up to 1100 mm. wide and 2 mm. or less in thickness which would be subsequently cold rolled in a single four-high stand. The effect of friction between the rolls and the strip and of roll diameter on the cold-rolling process is examined. R. A. R.

Cold Rolling—Developments in Modern Mill Design. L. R. Underwood. (Metal Industry, 1949, vol. 75, Sept. 9, pp. 203–206; Sept. 16, pp. 231–233; Sept. 23, pp. 243–245, 249; Sept. 30, pp. 270–273). The design of mills and auxiliary equipment for handling coils of strip is discussed and many types of coilers and decoilers, clutches, and tensioning gear for high-speed strip mills, mainly for non-ferrous metals are described. In the last part details of the V.S.G. pump for hydraulic drives, the Sendzimir mill, A. B. Montgomery's Y mill, and a new design of Steckel mill are given.—R. A. R.

Elongation and Change in Thickness when Rolling Seamless Steel Tubes in a Reducing Mill. W. Boettcher and A. Pomp. (Stahl und Eisen, 1949, vol. 69, Sept. 1, pp. 615–626). The factors affecting the deformation of steel tubes in a hot tube-reduction mill were investigated. These factors included the calibration of the rolls, the distance and tension between stands, the initial dimensions of the tube, and the rolling temperature. The wall thickness at the front end of the reduced tube was greater than the initial thickness, but that of the remainder of the tube was less. The increase at the front end depended solely on the change in outside diameter and the ratio of the initial wall thickness to the initial tube radius. A mill coefficient from which the expected elongation of a tube can be calculated is the ratio of the elongation at the front end to the elongation at the centre where the reduction in thickness is uniform. A reduction in thickness by tension depends upon the raw tube being gripped by several sets of rolls at the same time. When data for the tension are available curves can be constructed for the total elongation in relation to tension, and these can be used to calculate the rolling speed or the cross-section. Sets of three rolls spaced at 120° were found to be much more efficient than sets of two rolls at 180°.—R. A. R.

The Deformation of Tubes in a Single-Stand Reducing Mill (Sizing Mill). R. Hartjenstein. (Stahl und Eisen, 1949, vol. 69, Sept. 1, pp. 626–630). Some results of an investigation of the deformation of tubes in a sizing mill are reported. Deformation measurements included changes in length and thickness before and after rolling with four different passes, i.e., two-roll round, two-roll slightly oval, two-roll markedly oval, and four-roll round. The data obtained could be applied to improve the design not only of this old mill but to tube-reducing mills in general.—R. A. R.

X-Ray Gaging of Flat Rolled Steel. D. I. Brown. (Iron Age, 1949, vol. 164, Aug., pp. 101–104). Applications of X-ray thickness gauges for the checking of cut lengths of tinplate, and in the hot and cold rolling of sheets and bars are described. An accuracy of $\pm 1\%$ and the ability to measure up to $\frac{3}{8}$ in. are claimed.—J. P. S.

MACHINERY FOR IRON AND STEEL PLANT

Drives for Ore Dressing Plant. A. G. Arend. (Mine and Quarry Engineering, 1949, vol. 15, July, pp. 219–220). Recommendations are made on the selection of motors for driving the various units of an ore-dressing plant. Normal and high starting torques are dealt with, and it is shown where squirrel-cage, double-cage, and slip-ring motors can be used to the best advantage.—R. A. R.

Application of Electronics in the Iron and Steel Industry. W. M. McKie and D. A. Lamont. (Iron and Steel Engineer, 1949, vol. 26, Aug, pp. 59–67). Descriptions are given of electronic equipments that have been used in iron and steel works in recent years. These include motor controls, frequency changes, X-ray thickness gauges, and supersensitive pinhole detectors for tinplate.—J. C. R.

The Mission of Machinery Construction in the Safeguarding and Further Development of German Steel Production. P. Voltz. (Zeitschrift des Vereines deutscher Ingenieure, 1949, vol. 91, Sept. 1, pp. 436–442). Problems confronting

the German steel industry with respect to the most economical use of existing plant and the design of new machinery are discussed, with particular reference to large gas engines, Velox boiler installations, a Linde and Fränkl plant for producing oxygen of 73% purity, a two-high reversing billet mill, and a seamless tube reduction mill.—R. A. R.

Increasing the Efficiency of Highly Stressed Gears. H. M. Hiersig. (Stahl und Eisen, 1949, vol. 69, Sept. 29, pp. 695–701).

Steel Plant Operating Efficiency Improved by "Wireless" Telephones. E. F. Ross. (Steel, 1949, vol. 125, July 18, pp. 86–88, 126, 128, 130). The 'trolley phone' system used at a number of steel plants, ore unloading installations, and coke-oven batteries employs a frequency-modulated carrier, conveyed by the direct-current power lines. Headphones and loudspeaker are used for communication.—J. P. S.

Specifications for Electric Overhead Traveling Cranes for Steel Mill Service. (Iron and Steel Engineer, 1949, vol. 26, July, pp. CS1–CS46). This is the new revision of Association of Iron and Steel Engineers Standard No. 6 (revised May 1, 1949) on A.I.S.E. Crane Specifications.—J. C. R.

Magnetic Torque A-C Crane Control. F. M. Blum and F. W. Wendelburg. (Iron and Steel Engineer, 1949, vol. 26, July, pp. 59–68). The control described used an electromagnet to exert a torque on the motor of the crane. The wound rotor induction motor is considered to be the most desirable type for crane service.—J. C. R.

Overhead Travelling Cranes. J. W. Milner. (British Engineering, 1949, vol. 32, Sept., pp. 402–409). Several designs of electric overhead travelling cranes are described and illustrated and the main features to be sought when selecting a crane are pointed out.—R. A. R.

Sleeve Bearing Developments. A. B. Willi, jun. (Iron and Steel Engineer, 1949, vol. 26, Aug., pp. 68–74).

Materials Handling in the Wire Industry. (Wire and Wire Products, 1949, vol. 24, June, pp. 502–503). Brief descriptions are given of appliances for moving and lifting coils of wire.—R. A. R.

LUBRICANTS AND LUBRICATION

Surface Attack of Metals by Fatty Acids and the Formation of Lubricating Layers. D. Tabor and E. D. Tingle. (Société de Chimie Physique and the Faraday Society, Joint Meeting, Oct. 5–9, 1947: Surface Chemistry, 1949, pp. 217–222). It has generally been considered that boundary lubrication of solids is due to physical adsorption of lubricant molecules at the solid surfaces, and polar molecules were considered to be more effective than non-polar molecules since they are more strongly adsorbed. Recent work on metal surfaces proves this to be an over-simplification. On non-reactive surfaces (*e.g.*, platinum) fatty acids are no more effective as boundary lubricants than paraffins or alcohols of about the same chain length. On reactive metals however, friction and damage remain low at a much higher temperature. The breakdown temperature corresponds approximately to the melting or softening point of the metallic soap formed by chemical reaction at the surface. Lateral adhesion between the lubricant molecules is of primary importance in providing lubrication. Friction experiments on oxide-free surfaces suggest that fatty acids do not react directly with the metal to form metallic soap films, even with electronegative metals. Nor does it occur *via* the oxide film if this is formed in the absence of water; for adequate soap formation it is necessary for water to have been present during formation of the oxide film.—R. A. R.

Method of Determining the Suitability of Drawing Grease. F. Eisenkolb. (Archiv für Metallkunde, 1949, vol. 3, Aug., pp. 287–288). A method of determining the deep-drawing limit of steel sheets and its modification for the comparison of the friction-reducing properties of drawing lubricants are described. The maximum diameters of steel disc which could be deep-drawn without cracking using a punch 30 mm. in dia. with no lubricant, with vaseline, with an aqueous lubricant, and with graphite-containing lubricants were determined and demonstrated the superiority of the graphite lubricants. R. A. R.

The Measurement of Kinetic Boundary Friction or the Experimental Investigation of "Oiliness." J. R. Bristow. (Proceedings of the Institution of Mechanical Engineers, 1949, vol. 160, No. 3, pp. 384–402). *See* Journ. I. and S.I., 1950, vol. 164, Jan., p. 109.

Silicone. H. Kittel. (Metalloberfläche, 1949, vol. 3, Aug., pp. A-156–A-160). The paper summarizes the available information on the preparation and properties of silicone which is a polymeric organic compound containing –Si–O–Si–O group available as oil, grease, or resin, with unique resistance to heat and chemical attack. The lubricating silicone compounds are unique in that the viscosity changes little over a wide range of temperature. The silicone products of the Dow-Corning-Corporation (the only American producer), which include lubricants, greases, liquids for hydraulic apparatus, varnishes, and resins, are listed and their applications and properties given.—R. A. R.

WELDING AND FLAME-CUTTING

Metallurgical Aspects of Welding Mild Steel. N. Christensen. (Welding Journal, 1949, vol. 28, Aug. 3, pp. 373-s–380-s). The author discusses some of the factors affecting the choice of electrode coverings from the aspect of the relation between their slag-forming properties with iron, manganese, etc. Proper choice of covering can control, if not eliminate, weld porosity and hydrogen cracking.—J. P. S.

Time Studies in Electric Welding and Control of Output in Welding Plants. A. Kleandr. (Svařování, 1949, vol. 9, July, pp. 80–87). [In Czech]. The times necessary for carrying out individual operations of the welding process are analysed by the author as well as the influence of the various factors, *i.e.*, design, base material of the welded parts, type of electrodes used, desired weld quality, degree of skill of the welders, and equipment of the welding shop. He presents a method of determining the net welding time and auxiliary times and explains how the numerical values of the individual factors are determined experimentally and from graphs. This method can be used for determination of total welding times for individual mass-produced parts, and also for comparison of the times required for producing the same part by several different welding methods. Practical examples of calculation are included.—E. G.

The Arc Welding of Alloy Steel Sheets. H. von Conrady. (Die Technik, 1949, vol. 4, Aug., pp. 384–386). It is shown that it is not always necessary to use electrodes of the same composition as the parent metal when welding alloy steel sheets. The application of the finished part must be considered from the corrosion aspect, however, because there would be two dissimilar metals in contact.—R. A. R.

Flash Welding of Wide Strip Steel. F. R. Thompson and F. J. Waldschutz. (Iron and Steel Engineer, 1949, vol. 26, Aug., pp. 35–40). A detailed account is presented of operating practice with the flash welder which is capable of welding ordinary carbon steel from 12 to 96 in. wide and tinplate stock up to $\frac{5}{16}$ in. thick. The welded joints are strong enough to permit reducing strip in the tandem cold mills to one-tenth of the entering thickness, and to withstand any further working required for a finished sheet.—J. C. R.

Welded Shell Plates for Blast Furnaces. A. Nicaise. (Ossature Métallique, 1949, vol. 14, June, pp. 299–304). The author discusses the use of welded instead of cast steel plates in the construction of the bosh and hearth of blast-furnaces. The welded plates can be thinner and enable the brickwork to cool more rapidly than cast steel or riveted constructions, and the joints can expand and contract to some extent. The welding procedure is described in detail.—J. C. R.

Fatigue Strength of Fillet-Weld, Plug-Weld, and Slot-Weld Joints Connecting Steel Structural Members. W. M. Wilson, W. H. Munse, and W. H. Bruckner. (University of Illinois, 1949, Engineering Experiment Station Bulletin Series, No. 380). This investigation is a continuation of earlier work (*see* Journ. I. and S. I., 1944, No. II, p. 156A). The results indicated that the static strength of the parts connected with welded joints was not greatly affected by the weld, the lowest static strength reported being 54,400 lb./sq. in. in tension for channels connected to plates with longitudinal and transverse fillet welds. The fatigue strength of parts connected with welds was very low. The different designs of joint tested are described and the test data are given in numerous tables and graphs.—R. A. R.

Pressure Welding. (Welding, 1949, vol. 17, Aug., pp. 347–355). Pressure welding consists of heating the ends of plates, bars or tubes almost to fusion by oxy-acetylene torch, before compressing them together in an axial direction by hydraulic pressure. The process may be 'open butt' where the ends are heated from the end surface, or 'closed butt' where the ends are abutted before heating. Conditions for

operating the process are described, and examples of the properties of welded joints are given.—J. P. S.

Modern Method of Producing Steel Tubes. (Österreichischer Maschinenmarkt und Elektrowirtschaft, 1949, vol. 4, Aug. 31, pp. 287–288). The Sönnichsen-Rohrwalzwerk A.G., Oslo, has set up a new type of mill for making steel tubes from 19 to 50·8 mm. in dia. The raw material in the form of flat strip passes through four sets of rolls which shape it into a tube. The tube then passes under rotating disc electrodes in the welding machine to weld up the seam. Subsequent operations include scale removal in a water spray, shearing to length, cooling, straightening, and testing.—R. A. R.

Welding with the Aircomatic Process. E. H. Roper. (Welding Journal, 1949, vol. 28, Aug., pp. 728–730). The Aircomatic welding apparatus is a manually held 'pistol' using a gas-shielded metal arc between the work and the electrode which is in the form of a fine wire fed forward at high speeds. Helium or argon may be used as shielding gas. The process has so far only been used on aluminium, but modifications are in process to make it suitable for materials of higher melting point.—J. P. S.

Gas-Shielded Arc Welds Stainless Cups. C. Root. (Machinist, 1949, vol. 93, Oct. 29, pp. 960–961). The practice used for welding teat cups for milking machines by Michiana Products Corp. is described. Stainless steel is used for these cups, the barrel is curled in a punch press to form a tube, and the separate head is drawn out and pierced. Linde Heliarc welding equipment with argon gas is applied for welding the head and seams. A very clean weld which fulfils all the high sanitary requirements is obtained by this process. Two machines are used and the total welding time, not including loading and unloading, approximates 1½ min.

A Curious Appearance of the Microstructure of an Autogenous Weld. H. Figour. (Revue de Métallurgie, Mémoires, 1949, vol. 46, Sept., pp. 637–638). Oxy-acetylene and arc welds (the latter protected by an argon jet) were made on sheets of an iron–nickel–cobalt alloy, and after sectioning were etched with a mixture of nitric, glacial acetic, perchloric, and picric acids. The arc weld had a normal appearance, but the oxy-acetylene weld was covered with regularly arranged pits. The inclusions were on lines inclined at about 90° forming almost perfect squares with sides 15–16 microns long. The sides and summits of the squares were clearly in relief; the ordinary grain boundaries were etched out. The network was disturbed at grain boundaries, but where lines of the sub-structure crossed from one grain to another there was a change of orientation. This structure is attributed to a periodic precipitation of an impurity, which in this case must be an oxide.—A. E. C.

The Use of the The Hounsfield Tensometer in the Investigation of Welded Seams. M. J. Bottema. (Lastechniek, 1949, vol. 4, Sept., pp. 269–273). [In Dutch].

On the Weldability of High Strength Shipbuilding Steels. A. V. Nuñez. (Ingeniería Naval, 1948, vol. 16, Dec., p. 794 [in Spanish]: [Abstract] Journal of the British Shipbuilding Research Association, 1949, vol. 4, Mar., pp. 111–112). A comprehensive survey is presented of the various methods of testing the weldability of low-alloy steels used for hull construction. The author discusses the difficulties experienced with these steels and presents a diagram and table giving characteristics and values of the relative weldability of a number of European and American high-strength structural alloy steels. The diagram is based on Schnadt's investigations of normalized ferritic steels and shows curves of carbon and alloying constituents, the curve sheet being divided into zones of generally good weldability, of good weldability for thicknesses less than ⅜ in., and of bad weldability. There is a discussion on research on this subject in Great Britain, U.S.A., Germany, and Spain.

POWDER METALLURGY

Powder Metallurgy. J. F. Mills. (Mine and Quarry Engineering, 1949, vol. 15, June, pp. 175–181). The author briefly describes the preparation of metal powders, compacting, and sintering, and shows that powder metallurgy is advantageous only in the following cases: (1) Where melting is difficult or impossible; (2) to obtain a controlled degree of porosity; (3) to obtain certain combinations of properties by 'alloys' not otherwise obtainable; and (4) to save much costly machining. Within this framework six main types of product can be produced economically; they are: (*a*) the refractory metals, (b) superhard materials, (*c*) bearings and porous parts, (*d*) electrical contacts and magnetic parts, (*e*) machine parts, and (*f*) clad and duplicated parts. Examples of these are described.—R. A. R.

Powder Metallurgy—Survey of Advances Made from Pre-War Practice. H. W. Greenwood. (Metal Industry, 1949, vol. 75, July 8, pp. 32–33). Progress in powder metallurgy since 1939 has been spread over a wild field. This is briefly surveyed and special attention is given to cementation methods. Iron–copper compacts can be prepared by (*a*) diffusing copper into the pores by capillary attraction, from a molten bath; (*b*) heating iron pressings in contact with copper; (*c*) using copper-coated iron powder; and (*d*) using mixtures of iron and copper powders. As copper and iron form no brittle phases, strong sintered compacts over a wide range of copper/iron ratios can be made. With (*d*) the sintering temperature must be above the melting point of copper to obtain maximum increase in strength. Complex shapes can be constructed by brazing together parts made separately by powder metallurgy, the copper present providing the brazing metal. Copper–iron parts can also be heat-treated and case-hardened.—R. A. R.

Powder Metallurgical Processes. L. Aguet y Duran. (Revista de Ciencia Aplicada, 1949, vol. 3, Mar.–Apr., pp. 81–94; May–June, pp. 168–185; July–Aug., pp. 255–268). [In Spanish]. The first part of this paper defines the general conceptions of powder metallurgy with a brief historical sketch. The second and third parts deal with analysis, blending, classification, and the problems of compression in connection with presses and dies.—R. S.

Designing for Production by Powder Metallurgy. J. Bonanno. (Steel, 1949, vol. 125, Aug. 22, pp. 68–70, 72, 74, 77). An American mechanical toy company is now making a large number of parts by powder metallurgy, *e.g.*, motor armatures, wheels and the sides of locomotive bogies. Where production runs are long, tungsten carbide dies and punches are used. J. P. S.

Methods of Iron Powder Manufacture and Their Influence on Powder Properties. H. Bernstorff. (Metal Treatment and Drop Forging, 1949, vol. 16, Summer Issue, pp. 93–102). This is a translation by R. Hetzig of an article in Archiv für Metallkunde, 1948, vol. 2, Sept., pp. 289–295 (*see* Journ. I. and S.I., 1949, vol. 162, July, p. 364).

The Production and Properties of New Types of Sintered Alloys. (Impregnated Materials). R. Kieffer and S. Benesovsky. (Berg- und Hüttenmännische Monatshefte der Montanistische Hochschule in Leoben, 1949, vol. 94, Aug.–Sept., pp. 284–294). The production of alloys of immiscible metals by the immersion of a porous component of the higher-melting-point metal in the molten lower-melting-point metal is described and the technical requirements for the method to be successful are enumerated. The applications discussed include electrical contacts (*e.g.*, 18/8 stainless steel impregnated with copper and silver for the contacts of spot-welding electrodes) and bearings. The density, hardness, strength, and elongation of steels impregnated with copper, and of iron impregnated with lead, silver, copper, 80/20 copper–nickel, and 65/35 copper–manganese are given in tables.—R. A. R.

The Properties of Carburized Sintered Steel Parts. G. Will. (Stahl und Eisen, 1949, vol. 69, Sept. 1, pp. 630–635). This paper reports on an extension of the work of H. Wiemer and W. A. Fischer on carburizing parts made by powder metallurgy (*see* Journ. I. and S.I., 1948, vol. 160, Dec., p. 442). In the present experiments coal gas, hydrogen, and carbon monoxide mixtures, and additions of granular cast iron and carbon were employed as carburizing agents. The gases were unsatisfactory due to the slow diffusion rates. Good tensile strength (up to 42 kg./sq. mm. with 11% elongation) were obtained with cast iron and with carbon in powder form; the process was, however, slow as sintering for 24 hr. at 1300° C. had to be followed by annealing.—R. A. R.

Theory of Liquid-Type Sintering and the Effect of Pressure on Sintering. A. S. Berezhnoi. (Ogneupory, 1948, vol. 13, No. 8, pp. 351–361: American Ceramic Abstracts, 1949, Aug., p. 195).

PROPERTIES AND TESTS

The Application of Nuclear-Physical Methods in the Sphere of Iron and Steel. O. Rüdiger. (Stahl und Eisen, 1949, vol. 69, Sept. 15, pp. 671–676). The nuclear physical processes

which have application in iron and steel works laboratories are of three kinds, using (*a*) radio-active elements as indicators, (*b*) stable isotopes, and (*c*) neutron radiation. The production of radio-active elements and their concentration are discussed. The quantities required for an investigation can be estimated and formulæ for this are given. Safety precautions are dealt with. Some investigations are briefly described, including one on the determination of carbon in steel. The application of stable isotopes is recommended for determining the elements hydrogen, nitrogen, and oxygen. The application of neutron radiation extends to the non-destructive testing of materials, and to polarization experiments with ferromagnetic materials.—R. A. R.

New Dynamic Method for Measuring Elastic Constants. R. Cabarat. (Société Française de Métallurgie, Mar. 23, 1949: Revue de Métallurgie, Mémoires, 1949, vol. 46, Sept., pp. 617–621). The specimen, rod-shaped, about 200–250 mm. long and 8–12 mm. in dia., is supported at its mid-point and held vertically between two electrodes. The upper one receives the amplified potential from a low-frequency heterodyne generator and exerts a periodic electrostatic attraction on the end of the specimen, thus setting up longitudinal vibrations. The lower electrode acts as an electrostatic telephone; the potentials produced by the displacement of the specimen are amplified and measured on a cathode-ray oscilloscope. The specimen is earthed. The amplitude of the vibrations is large only when the frequency is the same as that of the specimen. By varying the frequency of the applied potential between the electrode and earth, resonance is set up; then the amplitude of the registered vibrations passes through a very marked maximum and the frequency read off on the dial of the generator is the same as that of the specimen in longitudinal vibration. The elasticity is calculated from the formula:

$$E = (2F_oL)^2\Delta,$$

where E is the modulus of elasticity, F_o the frequency, L the specimen length, and Δ the density. This formula takes no account of the radial contraction and expansion, but when the length is great in proportion to the diameter the correction becomes negligible. The method can also be used with non-conducting materials by applying a very thin conducting coating to the specimens; it can also be used to determine the damping capacity.—A. E. C.

The Theory of the Diagram of Additive Deformations and the Calculation of True Resistance to Rupture. V. Ya. Shekhter. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 957–961). [In Russian]. The concept of additive deformations (the natural logarithm of the ratio of the final and the initial length or cross-sectional area) is used for the derivation of equations for the calculation of the true resistance to rupture of the specimens, and the properties of the diagram relating these quantities are considered. The relationships obtained are applied to experimental data for steels, cast iron and non-ferrous metals, and calculated and experimental results are shown to be in good agreement.—S. K.

The Time Delay for the Initiation of Plastic Deformation at Rapidly Applied Constant Stress. D. S. Clark and D. S. Wood. (American Society for Testing Materials, 1949, Preprint No. 25). The design and construction of a special rapid-load testing machine is described; with it tensile loads may be applied to a specimen within five milliseconds and longer. Tests made on an annealed low-carbon steel are presented and discussed in detail. It is shown that a definite time delay is required for the initiation of plastic deformation in this material and that this delay depends on the applied stress. Experimental results obtained with three other steels and two aluminium alloys for which the stress-strain curve does not exhibit a definite yield are briefly presented. No time delay for the initiation of plastic deformation was observed for these. It is concluded that the time delay is associated with the yield point of mild steel.

A 1000-ton Horizontal Structure-Testing Machine. (Engineer, 1949, vol. 188, Nov. 18, pp. 576–579; Nov. 25, pp. 620–622). An illustrated description is given of a new structural testing machine built by W. and T. Avery, Ltd., Birmingham, for research into the strength of structural components of aircraft. The machine weighs some 250 tons and measures 75 ft. 6 in. long, 16 ft. 6 in. wide, and 15 ft. 1 in. high above ground level. It consists of a main frame in which a moving tension and compression crosshead is coupled by four tension bars to a moving ram crosshead. To this ram crosshead are coupled four packingless rams and cylinders, each of which is capable of a maximum effort of 250 tons.—R. A. R.

Formularisation of Stress-Strain Curves. Doris Crowden. (Metal Treatment and Drop Forging, 1949, vol. 16, Spring Issue, pp. 25–30, 37; Summer Issue, pp. 121–128, 130). Ways are explained in which stress-strain relations for metals having no true yield point, may be reduced to the terms of the equations of hyperbola, parabola, and ellipse, and these curves constructed. They can be used to determine the tangent modulus, equivalent to the modulus of elasticity at a given point, and the secant modulus, the product of a line through the origin and a point at the given stress.—J. P. S.

Dependency of Bending Angle on Specimen Thickness with Butt-Welded Bend-Test Specimens of Steel St 52. F. Bischof. (Werkstatt und Betrieb, 1949, vol. 82, July, pp. 237–238). Bend tests were made on butt-welded specimens of high-tensile steel (C 0·20%, Mn 0·99%, Cr 0·21%) 20, 30, 40, and 50 mm. thick. The greatest bending angle to appearance of the first crack was obtained with specimens 30 mm. thick. These results differed from those of N. Ludwig (*see* Journ. I. and S. I., 1948, vol. 160, Nov., p. 327).—R. A. R.

Transition Temperatures of Sub-Critical Zones of Welded Mild and D.W. Steels. (Admiralty Central Metallurgical Laboratory, Metallurgical Report DJX, ACSIL/ADM/49/471: [Abstract] Journal of The British Shipbuilding Research Association, 1949, vol. 4, Aug., p. 353). The transition-temperature ranges of the parent plate and the subcritically heated zones of automatic welds have been compared for coarse and fine-grained mild and D.W. steels. It is shown that for coarse and fine-grained mild steels the quench-ageing that has taken place in the subcritically heated zones has raised the transition-temperature range by a significant amount. No appreciable effect was observed with D.W. steels. Diamond-hardness surveys show for all steels the subcritically heated zones to be slightly harder than the parent plates as a result of quench-ageing. These results were obtained on ½-in. thick plate, and it is possible that the effects will be more pronounced when welding conditions permit a more rapid evolution of subcritically heated zones.

Causes of Cleavage Fracture in Ship Plate. Tests of Restrained Welded Specimens and Hatch Corner Specimens of Mild Steel. Progress Report. A. Boodberg and E. R. Parker. (National Research Council, Committee on Ship Construction, Division of Engineering and Industrial Research, Serial No. SSC–27, Aug., 1949: [Abstract] Journal of The British Shipbuilding Research Association, 1949, vol. 4, Oct., pp. 452–453). This report summarizes the results of a series of tests conducted as a supplementary programme to a research investigation that was carried out on the behaviour of high yield-strength structural steels for the U.S. Navy. The main objective was to provide a basis for comparison of results from the high yield-strength structural steels and the extensive results that were obtained in numerous investigations conducted on the ship-quality mild steels. The temperatures at which the mode of fracture changed from shear to cleavage type were determined for two of the mild steels by means of tests on several types of specimens. Several widely different types of specimens were used in this investigation. The first type was made in two sizes but with the same thickness of steel plate. They were made to provide restraint to plastic flows at a corner produced by welding together steel plates set along three mutually perpendicular planes. Other specimens were in the form of simple notched plates of different sizes. Tensile tests were made on ¾-in. thick flat-plate specimens. Some of these contained welds only along their longitudinal axes while others had both transverse and longitudinal welds. The results of the tests on the two sizes of large restrained welded specimens indicated that the width and height of this type of specimen apparently had little or no effect on the transition temperature. The results of tests performed on notched-plate specimens showed that the transition temperatures as determined by tests of the two sizes of this type of specimen were in good agreement with each other, but that the transition temperatures as determined by notched-plate tests were considerably lower than those obtained with large restrained welded specimen tests. Tension tests of simple welded flat plate specimens indicated that the transition temperatures of the steels were not materially affected by the introduction of a single longitudinal weld along the loading axis or by the presence of two intersecting welds at the centre of the unnotched plate specimen.

Notch-Sensitivity of Ship Plate—Correlation of Laboratory-Scale Tests with Large-Scale Plate Tests. N. A. Kahn and E. A. Imbembo. (American Society for Testing Materials, Symposium on Deformation of Metals as Related to Forming and Service, June 22, 1948: Special Technical Publication No. 87, pp. 15–52). *See* Journ. I. and S.I., 1948, vol. 159, Aug., p. 445.

Measurement of Ductility in Sheet Metals. J. R. Low, jun., and T. A. Prater. (American Society for Testing Materials, Symposium on Deformation of Metals as Related to Forming and Service, June 22, 1948: Special Technical Publication No. 87, pp. 53–65). Methods for the measurement of uniform elongation and the reduction of area in modified tension tests on sheet metals are described. The relationship between these two criteria and the forming limits in various forming operations, particularly stretching and bending, is discussed. Data are presented on the influence of specimen dimensions and lateral restraint on the elongation for various gauge lengths. The elongation/gauge-length curve is independent of the specimen width provided the specimen is long enough to ensure simple tensile loading within the gauge length.

R. A. R.

Hydraulic Bulge Testing of Sheet Metals. W. T. Lankford. (American Society for Testing Materials, Symposium on Deformation of Metals as Related to Forming and Service, June 22, 1948: Special Technical Publication No. 87, pp. 66–82). The hydraulic circular-bulge test for sheet metals has been studied and an elliptical-bulge test is described in which the specimen is stretched more in one direction than in the other. These tests have been applied to a study of the strain-hardening characteristics and ductility of aluminium alloy sheets.—R. A. R.

Notched Bar Tension Tests on Annealed Carbon Steel Specimens of Various Sizes and Contours. M. L. Fried and G. Sachs. (American Society for Testing Materials, Symposium on Deformation of Metals in Relation to Forming and Service, June 22, 1948: Special Technical Publication No. 87, pp. 83–117). The effects of section size and notch contour on the strength and fracture of a fully annealed 0·25% carbon steel have been studied. The fracture stress and notch ductility depended directly on the size and contour. The relative amounts of flat and shear areas for various sections and notch contours were obtained. The appearance of the fracture depended largely on the notch sharpness, and the shape during plastic flow depended essentially on the initial notch sharpness. A method, based on experimental data and simple geometry, was developed for accurately determining the distribution of the longitudinal, radial, and circumferential strains over the notched section.—R. A. R.

Residual Lattice Strains in Plastically Deformed Polycrystalline Metal Aggregates. G. B. Greenough. (Proceedings of the Royal Society, 1949, vol. 197, Series A, July 7, pp. 556–567). The author has examined by X-ray diffraction methods the residual lattice strain in plastically extended polycrystalline aggregates, and finds that the residual strains vary both in magnitude and in sign, if several diffraction lines be examined. One of the causes of internal stresses in plastically deformed polycrystalline aggregates is the variation with orientation of the yield tensions of the crystals, so that, as only grains with certain orientations contribute to a given diffraction line, the lattice strain measured is not the mean strain of all crystals, but only that of those with particular orientations, which may be positive or negative. This phenomenon may account for earlier unexplained discrepancies in the measurement of residual lattice strains.

J. P. S.

The Theory of Recovery. Doris Kuhlmann, G. Masing, and J. Raffelsieper. (Zeitschrift für Metallkunde, 1949, vol. 40, July, pp. 241–246). It is assumed that plastic deformation arises from dislocations which form at a limited number of centres. Since it is also assumed that some of the dislocations disappear during the deformation they accumulate on a slip plane. The magnitude of the forces acting between dislocations is obtained using G. J. Taylor's theory. Recovery takes place by the dislocations disappearing after removal of the load. Measurements of the recovery in aluminium single crystals agreed satisfactorily with the theory within the rather wide range of experimental error.—R. A. R.

Local Overstraining of Metals. F. Laszlo. (Engineering, 1949, vol. 168, Oct. 28, pp. 437–440; Nov. 4, pp. 468–470). Most of the paper is devoted to the great variety of harmful after-effects of local overstraining encountered in production processes and in machine servicing. The local-overstraining due to non-uniform heating has the most deleterious effects in production processes and machine servicing, and a qualitative analysis of the process is given which is followed by descriptions of many examples of the results of local overstraining and measures taken to counteract them.—R. A. R.

Investigation of Failures in Railroad Rails and Their Prevention. R. E. Cramer. (University of Illinois, 1949. Engineering Experiment Station Bulletin, Reprint Series, No. 43, pp. 3–7). The causes of 55 failures in service of control-cooled rails are analysed. Of these, 22 were transverse fissures from hot torn steel and 19 were fractures originating from shelling.—R. A. R.

Laboratory Tests of Two Welded Rails. R. E. Cramer. (University of Illinois, 1949, Engineering Experiment Station Bulletin, Reprint Series No. 43, pp. 8–10). Rolling-load tests have been made on welded joints and bolted joints in rails, and tensile and impact tests have been carried out on specimens cut from welded joints. In general the welded joints were superior in fatigue properties.—R. A. R.

Seventh Progress Report of the Rolling Load Tests of Joint Bars. R. S. Jensen. (University of Illinois, 1949, Engineering Experiment Station Bulletin, Reprint Series No. 43, pp. 11–26). Further rolling-load fatigue tests on fish plates are reported, in some of which the bars were kept wet. In general the fatigue life was greatly increased by shot-peening and by wider spacing of the bolt holes.—R. A. R.

Seven-Year Summary Report of Shelly Rail Investigation at the University of Illinois. R. E. Cramer. (University of Illinois, 1949, Engineering Experiment Station Bulletin, Reprint Series No. 43, pp. 26–32). Shelling cracks are primarily caused by the heavy bearing pressure of wagon wheels on a small area of contact which produce internal shearing stresses and flow that exceeds the capacity of steel to withstand them. Laboratory tests indicate that heat-treated rails should last about three times as long in severe service than standard carbon steel rails.—R. A. R.

Fatigue Tests of Manganese Steel. R. S. Jensen. (University of Illinois, 1949, Engineering Experiment Station Bulletin, Reprint Series No. 43, pp. 33–42). **T**-shaped specimens of manganese steel were subjected to compression-tension fatigue tests in the as-cast, ground, and shot-peened conditions. Similar specimens with and without protective coatings were also fatigue tested in 5% sulphuric acid. The endurance limits at 10 million cycles were: as-cast 38,000 lb./sq. in., ground 42,000 lb./sq. in., and shot-peened 48,000 lb./sq. in. For all three conditions in 5% sulphuric acid the stress/number-of-cycles curve approached a vertical line at about 10^6 cycles. The endurance limits of protected specimens at 10^6 cycles were from 65 to 87% as great as for specimens with no corrosion.—R. A. R.

Recent Progress in Metallography. G. Matte and R. Lachenaud. (Technique Moderne, 1949, vol. 41, July 1/15, p. 213: [Abstract] Journal of The British Shipbuilding Research Association, 1949, vol. 4, Sept., pp. 420–421). The authors describe briefly modifications they have introduced into the fluorescent method for testing small machine parts, which both simplify and speed up the procedure. In the case of parts of intricate shape with re-entrant angles or holes of small diameter, considerable difficulty is experienced in removing the last traces of the fluorescent liquid after the part has been immersed. The presence of traces of the liquid when the part is either coated with pulverized silica, or immersed in, or coated with, a colloidal solution of silica, hinders the detection of cracks by causing spurious fluorescence. This difficulty has been overcome by the addition of a wetting agent to the fluorescent solution. Dusting the surface with powdered silica presents no difficulties, but the process has been speeded up by immersing the parts in gel 149, a colloidal solution of silica in methanol, after cleaning subsequent to treatment with the fluorescent solution. The methanol evaporates almost instantaneously, leaving a white, finely divided, and strongly adhering, film of silica. Interesting results were also obtained by applying the gel 149 by means of a pistol. The best procedure for any particular case depends on the size and shape of the parts to be examined. The authors claim that they have been able to detect cracks of the order of 0·0004 in.

Black Light Inspection of Castings. F. Catlin. (Foundry, 1949, Aug., pp. 168–170, 172, 174). An account is given of

the Zyglo or 'black light' non-destructive method of inspecting castings. A fluid which has penetrative and fluorescent properties is applied to the part by dipping, brushing or spraying. After a short period the excess fluid is washed off with clear water and the part is dried thoroughly. A developing powder is then applied which draws the penetrant out of the cracks so that they become visible when the part is taken into a dark room and examined under 'black light' *i.e.*, near-ultraviolet light in the 3200–4000 Å range of wavelength).—J. C. R.

White Magnetic Fluid for Crack Detection. (Engineer, 1949, vol. 188, Nov. 4, p. 533). To render the indication of cracks by the magnetic powder method more distinct, Metropolitan-Vickers Electrical Co., Ltd. have developed a suspension prepared by ball-milling a mixture of equal parts by volume of aluminium and iron oxide powder and mixing the product in paraffin or methylated spirit. The surface cracks are shown as white lines by the aluminium particles which are carried to them by the magnetic powder.—R. A. R.

Reducing Cost of Fatigue Testing. H. J. Maier. (Machine Design, 1949, vol. 21, Sept., pp. 137–139). A modification of the usual type of rotating cantilever fatigue-testing technique has been devised to reduce the time and cost of fatigue testing. The principle adopted is to mark off a number of equidistant test points along the specimen from the face of the collet chuck towards the applied load. The specimen is run to fracture; the end is removed from the collet and the specimen re-inserted up to the second test point. As the distance from the load to this point and the load itself remain the same, the stress is also the same, and the revolutions to fracture at this stress is the sum of the revolutions in the first two stages of the test. The procedure is continued until the stress is so low that the accumulated revolutions without fracture is, say, 10^6. In applying the principle it was necessary to prevent the specimen breaking in the collet chuck because of clamping stresses, and the final design included machining a succession of shallow depressions at equal distances along the specimen and applying static tilting forces on the bearing carrying the load.—R. A. R.

Material Testing and Strength Calculation (Strength Due to Shape). G. Fiek. (Archiv für Metallkunde, 1949, vol. 3, Aug., pp. 271–273). Concepts arising in the calculation of maximum permissible loads on steel structures are explained. A notch has a marked effect on the fatigue strength and a notch factor is calculated by dividing the fatigue strength of the smooth part by that of the notched part. Besides this, however, there is the effect of size and a method of calculating this from test data on specimens without the need for full-scale tests is explained. This method takes into account the reduction in stress from the surface to the core of the specimen. R. A. R.

The Influence of Fluctuations in Stress Amplitude on the Fatigue of Metals. T. J. Dolan, F. E. Richart, jun., and C. E. Work. (American Society for Testing Materials, 1949, Preprint No. 32). The authors have studied the effect on fatigue life of three steels, S.A.E. 1045, 2340 and 4340, and an aluminium alloy, produced by several types of stress history in which the amplitude of stress was varied at regular intervals between two predetermined values. Both notched and unnotched small-size specimens were tested in rotating-beam fatigue machines; most of the data were obtained with a loading cycle in which 1000 cycles of a high stress amplitude were repeatedly alternated with 9000 cycles of a lower stress amplitude. The results are presented in the form of tables and graphs and are discussed in detail.

Contribution on the Method of Determining Fatigue Strength with High Stresses. S. L. Zhukov. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 971–976). [In Russian]. Methods are described by which the fatigue strength of normalized or hardened and tempered specimens of several steels were determined using Schenck's fatigue-testing machines. The determination of bending fatigue strength was carried out by finding the energy expended in turning the bent specimen, while the torsional fatigue strength was calculated from the area of the hysteresis loop. The damping capacities of the steels can be calculated from the fatigue values thus obtained.—S. K.

Method for Fatigue-Testing Metals with a Three-Roller Machine. M. M. Khrushchov and M. A. Babichev. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 962–967). [In Russian]. In the fatigue-testing machine described, a ring-shaped specimen is rotated between three rollers, arranged symmetrically, having diameters equal to that of the specimen, one of which is electrically driven. The load applied to the specimen through the driven roller can be as much as 100 kg., and the rate of rotation is 1000 or 1500 r.p.m. The determination of the fatigue limit of steel and other metals by this method is described.—S. K.

The Effect of Notches under Fluctuating-Load Conditions. E. Siebel and H. O. Meuth. (Zeitschrift der Vereines deutscher Ingenieure, 1949, vol. 91, July 1, pp. 319–323). A method of calculating the conditions of stress in a notched structure is explained; it is based on the stress distribution in bars of infinite width with notches of different depths.—R. A. R.

Fatigue Tests on Hard-Chromium-Plated Specimens. W. Eilender, H. Arend, and E. Schmidtmann. (Metalloberfläche, 1949, vol. 3, Aug., pp. A-161–A-163). It was previously found by H. Wiegand and R. Scheinost that chromium plating 0·035 to 0·175 mm. thick reduced the fatigue strength of steel bars (*see* Journ. I. and S.I., 1939, No. II, p. 165A); the present investigation was intended to check their results and establish the cause. The results were confirmed. The plating process did not damage the core material by causing it to absorb hydrogen, but the alternating stresses produced a network of cracks in the plating and led to a roughening of the steel at the interface, which in turn had a notch effect. A low-temperature tempering increased the fall in the fatigue strength.—R. A. R.

The Effects of Metallizing Procedures on the Fatigue Properties of Steel. W. L. Williams. (American Society for Testing Materials, 1949, Preprint No. 20). This paper presents the results of part of an investigation to determine the effects of metallizing and attendant methods of surface preparation on the bending and torsional fatigues properties of steel. A normalized and tempered nickel–vanadium steel was used and four methods of surface roughening, electric bonding, grit blasting, rough threading, and grooving and knurling, were investigated. The use of shot-peening in conjunction with electric bonding was also examined. An analysis was made to show the principal reasons for the observed reductions in fatigue strength and the way in which these are related to the size of the tested section. The grit blast process is preferred for all applications where the bond strength is adequate. The electric bond method is the least desirable from the standpoint of stress concentration, but is preferred over grooving or threading on small parts due to the small undercut required. The rough threading procedure is less damaging than grooving and knurling for parts subjected to bending stresses, although both exert about the same effects in torsion.

The Alternating Bend Test for Haulage Cable Wires. H. Herbst. (Iron and Steel Institute, 1949, Translation Series, No. 392). This is an English translation of a paper which appeared in Glückauf, 1924, vol. 60, Nov. 29, pp. 1111–1120 (*see* Journ. I. and S.I., 1925, No. I, p. 560).

A Mechanical Test to Measure the Workability of Wire. W. Chubb, jun., and D. S. Eppelsheimer. (Wire and Wire Products, 1949, vol. 24, June, pp. 491–493, 545, 546). A simple test procedure capable of testing very small differences in the workability and stiffness of wires is described. The apparatus consists of a steel yoke and a tensile-testing machine with a load range of 0–100 lb. and a loading rate of less than ½ in./min. The yoke consists of two rectangular pillars about 3½ in. high separated by shims to a distance equal to eight times the diameter of the wire to be tested; two bolts pass through the pillars and shims. A ⅜-in. bar passes through opposing holes in the two pillars above the shims. Pairs of holes are drilled below the shims and these are 0·02 in. larger than the diameters of the wires to be tested. A loop of heavy wire round the ⅜-in. bar is gripped in the upper jaws of the tensile machine, and, after resting the specimen across the yoke in the two appropriate holes, a loop of wire of the same gauge as the specimen is passed round the latter and into the lower jaws. Load is then slowly applied until the centre of the specimen is deflected a distance of four times its diameter. The maximum load applied up to this point is the value of the 'crimp sensitivity.'—R. A. R.

Static *vs.* Dynamic Test Methods. J. N. Kenyon. (Wire and Wire Products, 1949, vol. 24, June, pp. 498–500, 525, 527). Progress in the development of static and dynamic testing machines for wire is reviewed, very brief descriptions being given of the Woernle, Deforest-Hopkins, Shelton, and

Haigh-Robertson fatigue-testing machines and the Kenyon rotating wire arc method.—R. A. R.

A Microhardness Tester for Use with an Inverted Microscope. G. H. Townend. (Journal of Scientific Instruments, 1949, vol. 26, Sept., pp. 295–296). General aspects of microhardness tester design are discussed and an instrument is described which has a standard Vickers pyramid indenter supported by two beryllium-copper corrugated diaphragms; these diaphragms constrain the diamond along a vertical line and serve to measure the applied load. This instrument is interchangeable with the objective of a vertical projection microscope and is inserted in place of it when the spot to be examined has been adjusted to the centre of the field.—R. A. R.

A Portable Hardness Tester. (Engineer, 1949, vol. 188. Nov. 18, p. 591). The Swiss 'Ernst' portable hand-operated hardness tester (now made in England by Snow, Deakin and Co., Ltd., Bideford) is described. It has a range of 100 to 400 Brinell. The movement of the indenter is transmitted to a diaphragm above which is a vacuum-filled liquid chamber in direct communication with a small opening in a transparent capillary tube. The zero mark on the scale is set before testing to coincide with the end of the coloured liquid in this tube, and in the test the hardness is indicated by the movement of the liquid.—R. A. R.

Interferometric Examination of Hardness Test Indentations. S. Tolansky and D. G. Nichols. (Nature, 1949, vol. 164, July 16, p. 103–104). The authors have carried out experiments to determine the distortion produced by standard diamond hardness indenters (Vickers pyramid, Rockwell cone, and Knoop pyramid). After impressions have been made on ground and polished surfaces, a silvered optical flat is brought up to them and either monochromatic or white light used to produce interference fringes. Considerable distortion is found in some cases, and among interesting suggestions is one that the 'kite-shaped' diamond-impression familiar to metallographers is due, not to non-perpendicular indentation, but to local irregularities partly of surface origin. J. P. S.

Systematizing the Alloys. R. Mitsche. (Archiv für Metallkunde, 1949, vol. 3, Aug., pp. 299–300). The mechanical properties of an alloy are affected by the properties of the individual phases, the relative proportions of the phases, and their distribution. These factors are discussed and a system of arrangement is suggested in which the polycrystalline hardness of a supersaturated solid solution, as measured by the Brinell or Vickers methods, increases with diminishing particle size and reaches a maximum with the formation of a 'cloud' of particles of the component present in excess; beyond this point the particles form their own phase and the hardness decreases.—R. A. R.

Mechanical Behaviour of Isotropic Polycrystalline Metals. Analogy of the Embrittling Factors. A. Gueussier and R. Castro. (Société Française de Métallurgie, Oct. 20, 1948: Revue de Métallurgie, Mémoires, 1949, vol. 46, Aug., pp. 517–536). The authors have studied the influence, individually or together, of the three essential parameters in the elastic and plastic deformation of istropic metals, in particular ordinary and special steels. These parameters are: (1) The state of the stresses, expressed by the index of triaxiality; (2) the inverse of the temperature of deformation; and (3) the rate of deformation; they are grouped under the term 'embrittling factors.' A rational method of varying the triaxiality index of stresses was adopted for the impact test. A close analogy was found between the variation of the specific work of rupture as a function of one or other of these factors, and the experimental results relating to this work could be regrouped with those already published. A general expression for the laws of deformation in isotropic metallic materials, in which the three embrittling factors play a symmetrical rôle, is proposed. By using a new interpretation of the phenomenon of necking, the results of other investigators who have studied the tensile test can be correlated with the general law now proposed.—A. E. C.

Magnetic Investigations of Low-Carbon Iron–Chromium Alloys. H. Fahlenbrach. (Archiv für das Eisenhüttenwesen, 1949, vol. 20, Sept.–Oct., pp. 293–299). The permeability at low field strengths and the coercive force were determined in low-carbon iron–chromium alloys with chromium up to 30%, aluminium 0–7%, and silicon 0–5%, annealed at 900° and 1100° C. In agreement with theory, these alloys are not magnetically soft. The low permeability at 10% chromium was very marked. As the permeability of these alloys increases very little at low field strengths they can be used as substitutes for iron–nickel alloys. The high specific electrical resistance of very thin sections of iron–chromium–aluminium–silicon alloys gives them advantages for magnets in high-frequency apparatus. By hard rolling iron–chromium alloys, magnets with constant and stable permeability and less hysteresis loss can be produced.—R. A. R.

The Magnetic Properties and the Application Possibilities of Permanent Magnets Made by Powder Metallurgy. H. Fahlenbrach. (Archiv für das Eisenhüttenwesen, 1949, vol. 20, Sept.–Oct., pp. 301–304). The theory of the effects of porosity and non-magnetic inclusions on the magnetic properties of aluminium–nickel and aluminium–nickel–cobalt magnets made by powder metallurgy is considered together with magnetic test data. The effects noted are mainly due to the increase in cross-section. Examples are given of (1) magnets with increased mechanical strength made by inserting one or two iron pins in the compact before sintering, and (2) disc magnets 0·4 mm. thick.—R. A. R.

Effect of Tension on Magnetization Curves. K. H. Stewart. (British Electrical and Allied Industries Research Association, 1949, July 7, Technical Report N/T47). An account is given of the process of magnetization in a single crystal specimen which has a tension applied in the same direction as the magnetic field. It is shown that by considering fully the effects of demagnetizing fields, results are obtained which agree well with experiment and remove some discrepancies of the older domain theory.

Progress in the Sphere of Magnetic Materials. F. Pawlek. (Metallwirtschaft, 1949, vol. 3, July, pp. 211–215). Progress since 1939 in the development of magnetic materials is reviewed. The bibliography contains 65 references.—R. A. R.

Magnetic Properties of Iron Compacts in Relation to Sintering Temperature. R. Steinitz. (Journal of Applied Physics, 1949, vol 20, July, pp. 712–714). The magnetic permeabilities of ring-shaped iron compacts were measured after sintering at 1250° and 1350° C. for 24 hr., and compared with values for samples sintered at lower temperature. A considerable increase in the permeability for identical densities was found for most of the powders. The increase in permeability is explained by the theory of Polder and Van Santen, if it is assumed that the pores change in shape from flat discs to approximately spheroids at the high sintering temperature.

Testing Magnetic Materials. B. M. Smith. (American Society for Testing Materials, Symposium on Magnetic Testing, 1948, Special Technical Publication No. 85, pp. 3–16). The paper describes briefly some of the accepted methods and instruments commonly used by the electrical manufacturing industry in the development, design, and control of magnetic materials. Consideration is given to materials with properties which are very difficult to measure, sources of error, and precautions to be taken.—R. A. R.

Permanent Magnet Test Methods and Their Validity in Determining Product Performance. C. A. Maynard and J. E. Mitch. (American Society for Testing Materials, Symposium on Magnetic Testing, 1948, Special Technical Publication No. 85, pp. 17–29). Factors affecting the selection of suitable tests to ensure validity between performance of the product and magnetic test data are discussed and methods of testing are described.—R. A. R.

Core Loss Test for Narrow Silicon Steel Strip. J. A. Ashworth. (American Society for Testing Materials, Symposium on Magnetic Testing, 1948, Special Technical Publication No. 85, pp. 30–38). The paper describes a test procedure for determining the cross-grain losses (transverse to the rolling direction) in cold-rolled silicon steel strip from 11 down to 3¼ in. wide at 60 cycles and 10 kilogauss with an accuracy sufficient for most engineering or quality control purposes. The method is simply the Epstein method modified by decreasing the lengths of the test windings and specimens.—R.A.R.

D-C. Permeability Testing of Epstein Samples with Double-Lap Joints. D. C. Dieterly. (American Society for Testing Materials, Symposium on Magnetic Testing, 1948, Special Technical Publication No. 85, pp. 39–62). Results are reported on a series of tests made to determine the correct magnetic path length when D.C. permeability tests are made on Epstein strips with double-lap joints. When suitable assumptions for path length are made, the accuracy of D.C. permeability measurements in the 25-cm. Epstein frame compares very favourably with ring tests.—R. A. R.

A New D-C. Permeameter. W. J. Carr, jun. (American Society for Testing Materials, Symposium on Magnetic Testing, 1948, Special Technical Publication No. 85, pp. 63–81). A new type of D.C. permeameter for testing soft magnetic material is described in which tests can be made on single strips and rods much shorter than those previously required. Measurements can also be made on certain samples of irregular shape providing they are uniform over a length of about 1 in. Values of the magnetizing force H from about 10^{-3} to 10^{2} oersteds can be measured.—R. A. R.

Variation of Core Loss and Permeability of Electric Grade Silicon Sheet Steel. A. C. Beiler and P. L. Schmidt. (American Society for Testing Materials, Symposium on Magnetic Testing, 1948, Special Technical Publication No. 85, pp. 82–95). Statistical methods have been applied to the analysis of frequency distributions of core loss and permeability test values obtained from suppliers' reports of single tests on 11,000-lb. lots of electric grade sheet steel over a considerable period of time. The same analysis has been applied to data from 60 tests from each of several lots of electric grade steel. The variability within a lot of material is substantially the same as that from lot to lot according to the single test.—R. A. R.

The Evaluation of Hysteresis Core Loss by Power Equations. H. W. Lamson. (American Society for Testing Materials, Symposium on Magnetic Testing, 1948, Special Technical Publication No. 85, pp. 96–112). A study was made of the exponents and coefficients occurring in the Steinmetz and Rayleigh equations for hysteresis core loss. The significance of the exponents in the domain theory was investigated.

Magnetic Analysis Inspection in the Steel Industry. T. Zuschlag. (American Society for Testing Materials, Symposium on Magnetic Testing, 1948, Special Technical Publication No. 85, pp. 113–122). The operating principles, equipment, and methods for the magnetic testing of steel are reviewed.—R. A. R.

Magnetic Stress Analysis. P. E. Cavanagh and T. Wlodek. (American Society for Testing Materials, Symposium on Magnetic Testing, 1948, Special Technical Publication No. 85, pp. 123–140). The theory of magnetic stress analysis for determining the internal microstresses in metals is explained. The Dyna-magnetic analyser for measuring these stresses or stress changes in parts which can be subjected to a small dynamic load is described: it can also be used to calibrate other magnetic testing instruments so that they will make quantitative determinations of internal microstresses under specific conditions. Applying the method to fatigue tests it was found that internal microstress increases at loads below the endurance limit, opposing slip and cold working; these build up during subsequent cold working to a level preventing further slip. Under these circumstances the metal will not fail in straight fatigue.

In metal loaded dynamically above the endurance limit, no change in microstresses could be detected immediately after applying the load. The internal stresses allowed slip to begin only after the first few thousand stress cycles; slip began slowly, continued rapidly, and then slowed down. The true elastic limit might be defined as the stress at which the internal microstresses begin to increase with increasing dynamic load. This is well below the limit of proportionality where irreversible macroscopic strain is detectable mechanically.—R. A. R.

The Testing of Magnetic Recording Media. D. E. Wiegand. (American Society for Testing Materials, Symposium on Magnetic Testing, 1948, Special Technical Publication No. 85, pp. 141–153). The three types of measurement used by the Armour Research Foundation in evaluating experimental magnetic recording media are described. These are listening tests, and measurements of the final performance characteristics and the basic magnetic properties.—R. A. R.

Contributions to the Data on Theoretical Metallurgy. X. High-Temperature Heat Content, Heat Capacity, and Entropy Data for Inorganic Compounds. K. K. Kelley. (United States Bureau of Mines, 1949, Bulletin 476). This work brings together and correlates all available high-temperature heat-content and specific-heat data for inorganic substances and formulates tables and algebraic expressions for their representation. Seven pages are devoted to iron, ferrous and ferric oxides, magnetite, and the sulphide, carbide, silicide, chloride, chromite, silicate, and titanate of iron.—R. A. R.

Notes on Experimental Methods and Systematization of the Results when Studying Molten Metals and Alloys. F. Sauerwald. (Berg- und Hüttenmännische Monatshefte der Montanistische Hochschule in Leoben, 1949, vol. 94, Aug.–Sept., pp. 295–301). The properties of molten metals which distinguish them from those of other liquids, and methods of determining them, are discussed with special reference to internal friction, density, surface tension, and heat of combination. It is shown that alloys can be classified in two groups according to whether the curves for the properties in the liquid state plotted against composition more or less coincide with, or deviate widely from, the mean curve for the properties of the separate components. A statistical analysis of 300 systems proved the validity of this classification.—R. A. R.

The Theory of Ultrasonic Materials Testing. H. E. Van Valkenburg. (Mechanical Engineering, 1949, vol. 71, Oct., pp. 817–820). The theory of the testing of materials by ultrasonic methods is outlined. Ultrasonics are non-destructive and enable elastic properties to be studied, thickness to be measured and flaws to be detected and located. The ultrasonic beam travels several feet before its energy is dissipated, exceeding the range of other testing methods. The method can be applied to a variety of contours, dimensions, surfaces and thicknesses, and an instantaneous indication obtained.

The Practicability of Using Ultra-Sonic Images for the Non-Destructive Testing of Materials. R. Pohlman. (Schweizerische Bauzeitung, 1949, vol. 67, Feb. 5, p. 85: [Abstract] Journal of The British Shipbuilding Research Association, 1949, vol. 4, July, pp. 322–323).

Dislocations as a Cause of Mechanical Damping in Metals. J. D. Eshelby. (Proceedings of the Royal Society, 1949, vol. 197, Series A, June 22, pp. 396–416). In a vibrating single crystal of a metal, an energy loss is observed, usually attributed to the movement of dislocations. The author proposes a mechanism to account for this. Dislocations are trapped in 'potential troughs' at the minima of the internal stresses in the crystal and, when the crystal vibrates, oscillate in these troughs. The moving stress system associated with them produces a fluctuating temperature distribution in the material: the resulting flow of heat leads to the dissipation of mechanical energy, as in Zener's theory of damping. In this paper the author calculates the rate of loss of energy produced by a dislocation oscillating with given amplitude and discusses the effect of a collection of dislocations. To estimate the damping in a vibrating crystal it is necessary to know (1) the relation between the amplitude of oscillation of a dislocation and the vibrational stress causing it to move and (2) the density of the dislocations in the material. This latter quantity is unknown, but the author shows that the damping depends only upon the ratio of the number of dislocations per unit area to the number of potential troughs per unit area. From the observed damping of single crystals of copper this is found to be of the order of unity. The present theory, indicating that the damping should increase with frequency, is in disagreement with the limited experimental data. The thermoelastic damping arising from interaction between the vibrational stresses and the stresses surrounding stationary dislocation, as well as the damping due to the omission of elastic waves from an oscillating dislocation, are both found to be small compared with the thermoelastic damping due to moving dislocations.—J. P. S.

Notes on the Use of Radon in Engineering Radiography. V. E. Pullin. (Engineer, 1949, vol. 188, Oct. 21, pp. 452–453). Recommendations are made on the safety measures to be adopted when using radon in engineering radiography.

Some Aspects of Metallurgical Research at High Temperatures. F. Adcock. (Australian Institute of Metals: Australasian Engineer, 1949, July 7, pp. 75–79). Beginning with Rosenhain's work in 1927 on iron alloys, the author briefly surveys the metallurgical research work at high temperatures which has since been carried out, particularly at the National Physical Laboratory. The preparation of high-purity iron, the failure of the carbon-ring resistor furnace, the development of the high-frequency furnace for vacuum melting, at first spark controlled, but later operated with a thermionic valve, difficulties overcome in melting under high vacuum, and dilatometric methods of studying the $\gamma \rightarrow \delta$ transformation of iron are briefly touched on. Work by the author's colleagues at the National Physical Laboratory on iron–manganese fusion analysis, and intercrystalline cracking of boiler plates alloys, refractories, vacuum is referred to.—R. A. R.

Creep-Testing Facilities Expanded by National Tube Research Laboratory. (Industrial Heating, 1949, vol. 16, July, pp. 1176–1180). The research laboratory of the National Tube Co., Pittsburgh, has equipped a creep-testing room with eighteen lever and two screw-driven testing machines. Thermocouple control of the electric furnaces enables the temperature to be kept within ± 2° F. of that specified. The creep is measured and recorded with electronically controlled motor-driven extensometers.—R. A. R.

On the Centrifugal Method of Testing Metals and Alloys at High Temperatures. M. E. Rabinovich. (Zavodskaya Laboratoriya, 1949, vol. 15, pp. 988–993). [In Russian]. Various methods of high-temperature testing of metals and alloys are discussed, and experiments are described in which the recently proposed centrifugal method of I. I. Kornilov was applied to specimens of some non-ferrous metals and alloys. On the basis of the results obtained and from theoretical considerations, relationships generally applicable to the testing of ferrous as well as non-ferrous metals are deduced.—S. K.

Creep and Relaxation of Metals at High Temperatures. A. E. Johnson. (British Electrical and Allied Industries Research Association: Engineering, 1949, vol. 168, Sept. 2, pp. 237–239). Factors which might influence creep at high temperatures are examined and the validity of the conclusions reached is checked against results of relaxation and normal creep tests carried out at the National Physical Laboratory. The tests were made on carbon steels and a bolt steel at temperatures up to 525° C. for periods up to nearly two years. In general, the results indicated that, within the range of conditions applied, the normal creep properties of these materials could not be used to predict their relaxation characteristics, and that the time- and strain-hardening theories of creep which were considered were not entirely satisfactory. R. A. R.

Some Experiences with the Creep Behaviour of Materials. A. Johansson. (Engineers' Digest, 1949, vol. 10, Oct., pp. 340–344). This is an English translation of an article which appeared in Swedish in Teknisk Tidskrift, 1949, vol. 79, Feb. 19, pp. 127–132.

Contribution to the Study of the Relation between Microstructure and Creep Resistance. Influence of the Load. G. Delbart and M. Ravery. (Comptes Rendus, 1949, vol. 229, Oct. 17, pp. 759–760). In a previous note the authors showed how the creep resistance of a chromium–molybdenum steel (C 0·15%, Cr + Mo 0·6%) varied with the microstructure. In the present communication the authors place the various structures in order of decreasing stability of creep resistance for a given temperature rise; this temperature stability of the creep resistance of each constituent increases as the transformation temperature of the austenite from which it was formed increases. The relation between the rate of creep and the load at constant temperature was also examined. The structures fell into two families: (*a*) those formed in the Ar′ and upper Ar″ ranges and which contained granular ferrite associated with the pearlite or the upper bainite, and (*b*) structures composed of lower-bainite or sorbite, without granular ferrite.—A. E. C.

Phase Changes in Adsorbed Films at the Gas/Solid Interface. S. J. Gregg. (Société de Chimie Physique and the Faraday Society, Joint Meeting, Oct. 5–9, 1947: Surface Chemistry, 1949, pp. 205–216). Films of gas adsorbed on solids can exist in a number of different states resembling the gaseous, liquid-expanded, liquid-intermediate and liquid-condensed states of films on water. These states undergo transformations into one another, the nature of which is elucidated by a study of the film compressibilities calculated from the adsorption isotherms; transformations of 'diffuse first order' seem particularly common.—R. A. R.

Diffusion in Metals and Alloys. J. D. Fast. (Metalen, 1949, vol. 3, May, pp. 191–195; June, pp. 215–222; July, pp. 239–248; Aug., pp. 261–270). [In Dutch]. A simple mathematical treatment of diffusion is given in Part I with definitions of the coefficient of diffusion. In Part II the statistical nature and the atomic theory of diffusion is discussed. Part III deals with the mechanism of diffusion in substitutional solid solutions, and Part IV with diffusion in binary solid solutions; the detrimental effect of hydrogen in iron and steel is also dealt with.—R. A. R.

Effect of Aluminum and Vanadium on Toughness of High Hardenability Cast Steels. J. F. Wallace. (American Foundrymen's Society, May, 1949, Preprint No. 49–2). The object of this investigation was to compare the effect of aluminium and vanadium, when used as grain refiners, on the toughness of fully quenched and tempered samples from three cast steels of high hardenability. Induction furnace heats of three high-hardenability steels were melted from high quality materials. Each heat was divided into two parts and cast into two separate chill moulds; one ingot from each heat employed aluminium and the other vanadium as a grain refiner. Small pieces for **V**-notch Charpy impact and tensile tests were cut from the six ingots, water-quenched, and tempered over a hardness range of 28–38 Rockwell C. All impact tests were carried out in duplicate: half the bars were quenched from each tempering temperature (to avoid temper embrittlement) and the other half furnace-cooled. The specimens were fractured at temperatures varying from + 150° to − 195° C. Chemical compositions were all low in carbon and included low, medium, and high manganese steels alloyed with varying amounts of nickel, chromium, and molybdenum to impart sufficient hardenability for fully hardening heavy sections. The relative effect of aluminium and vanadium on toughness was obtained by comparing the impact test data of the aluminium-refined ingot with that of the vanadium-refined ingot from the same heat at equivalent strength levels. Curves plotted from resistance to impact fracture against testing temperature data for the three heats at equivalent hardness levels indicated: (1) Aluminium imparted superior toughness to low and medium manganese steels; and (2) vanadium gave better impact resistance to high-manganese steel. The difference in toughness produced by using vanadium or aluminium as grain refiners decreased as hardness increased. Comparison of the aluminium-refined ingots showed that the medium-manganese heat had a low transition temperature, followed closely by the low-manganese heat, with the high-manganese heat showing brittle fractures at much higher temperatures. Slow cooling from the tempering temperature did not greatly affect the low and medium manganese heats but caused considerable embrittlement of the high-manganese heat. The slightly lower toughness of the aluminium-killed, low-manganese steel may be the result of its lower residual aluminium content and the inferior toughness of the higher manganese heat could have been produced by the higher manganese and chromium contents.

Quality Control in Steelworks. W. Küntscher. (Die Technik, 1949, vol. 4, Sept., pp. 425–432). Methods of establishing the causes of rejects at different stages of manufacture, and the significance of the contents of the different elements in steel in relation to its properties are discussed.—R. A. R.

Influence of Chromium on the Graphitization of White Cast Iron. G. Joly. (Fonderie, 1949, Apr. pp. 1537–1544). This is the French text of a paper presented to the American Foundrymen's Association (*see* Journ. I. and S.I., 1949, vol. 161, Jan., p. 75).

The Effects of Zirconium in Cast Iron. C. M. Offenhauer and J. F. Collins. (Canadian Mining and Metallurgical Bulletin, 1949, vol. 42, July, pp. 338–350). Zirconium may promote the formation of normal flake graphite, fine flakes of eutectiform graphite, or spheroidal graphite in cast iron, according to the composition of the iron and the amount added. It is most effective as an inoculant for producing pearlitic high-strength cast iron when added in the form of a silicon–manganese–zirconium alloy; in this form it reduces chill and minimizes zones of ferrite and eutectiform graphite. In hypereutectic low-sulphur cast iron, zirconium causes a partially nodular graphite structure to form. A combination of magnesium with silicon–zirconium alloy produces completely nodular graphite without the risks associated with the addition of elemental magnesium. The paper includes data on the compositions, mechanical properties, and percentage recoveries of zirconium and magnesium.—R. A. R.

Behaviour of a Steel–Aluminium Wire Used as a Contact Line after 175,000 Passages of the Trolley Wheel. L. Albert. (Revue de l'Aluminium, 1948, vol. 25, Nov., pp. 339–342). After 175,000 passages of the wheel at the top of the trolley-bus arm, the wear on a composite steel–aluminium overhead wire was not of measurable dimensions. The performance of this wire compared very favourably with that of electrolytic copper and cadmium–copper wires.—J. C. R.

The Manufacture and Testing of Steel–Aluminium Contact Wires. L. Albert. (Revue de l'Aluminium, 1949, vol. 26, May, pp. 178–181). This is an account of the specifications to which composite steel–aluminium wires, used as trolley

lines for tramcars and trolley-buses, must conform in France. J. C. R.

The Rôle of Cobalt in Special Steels and in Alloys. A Michel. (Technique et Science Aéronautique, 1948, No. 3, pp. 164–168). The use of cobalt in steels and alloys is reviewed with reference to high-speed and tool steels, and to magnetic, heat-resisting refractory, and special alloys.—R. F. F.

Effect of Sulphur on Quality and End Uses of Steel Products. M. Tenenbaum. (American Iron and Steel Institute, 1949, May, Preprint). The first part of the paper summarizes the available information on the effect of sulphur on the mechanical properties, weldability and corrosion resistance of steel. The second part contains an analysis of the answers to a questionnaire on the effect of sulphur on the quality of various end products. The maximum permissible sulphur contents for a large number of products are shown. Sulphur appears to have a greater effect on the surface of killed drawing-quality steels than it does on rimming steels.—R. A. R.

Common Alloy Steels—Their Characteristics and Uses. (SAE Journal, 1949, vol. 57, Oct., pp. 51–53, 60). This paper, prepared by the Alloy Steels Division of the SAE Iron and Steel Technical Committee, discusses the core- and case-hardening properties of a large number of SAE alloy steels. R. A. R.

Recent Developments in Special Steels. C. H. Desch. (Mémoires de la Société des Ingénieurs Civils de France, 1948, Centenary Issue, Part II, pp. 190–199). Some wartime developments in special steels and their fields of application are reviewed.—J. C. R.

The Application of Special Steels to the Construction of Chemical Plant. W. Tofaute. (Chemie, Ingenieur, Technik, 1949, vol 21, July, pp. 269–270). It is shown how expensive elements can be saved by using carbon steel clad with 18/8 stainless steel instead of 100% stainless steel for acid-resisting vessels in the chemical industry.—R. A. R.

Steel for Gears. F. Staub. (Hutnik, 1949, vol. 16, May–June, pp. 184–187). [In Polish]. In the manufacture of gears the most essential factors are the choice of steel and the methods of heat-treatment. The author suggests a plain 0·8% carbon steel with varying amount of manganese depending on the size of gear. Some instructions for the heat-treatment are given.—W. J. W.

Tensile and Notched-Bar Impact Tests on Chromium–Nickel–Molybdenum, Chromium–Molybdenum, and Chromium–Vanadium Steels in the Hot and Cold States. A. Pomp and A. Krisch. (Max-Planck-Institut für Eisenforschung : Archiv für das Eisenhüttenwesen, 1949, vol. 20, Sept.–Oct., pp. 323–328). Tests on specimens cut in different directions from forgings of the above low-alloy steels showed that there was next to no difference in the elastic limit and tensile strength in the longitudinal and tangential directions, but in the latter direction the elongation and reduction in area were lower. This difference was more marked in impact tests. The tensile test data on tangential specimens at temperatures from −50° to +600° C. showed a very steady change. There was, however, considerable scatter in the elongation data at above 400° C. Several compositions had their maximum tensile strength at 300° C. Impact strength of tangential specimens was scarcely affected by temperature in the + 50° to + 250° C. range. The impact strength in general fell with decreasing temperature in the +50° to −50° C. range, but there was no mathematical relationship.—R. A. R.

The Effect of Special Nitrides on the Properties of Low-Tungsten High-Speed Steels. F. Rapatz and J. Frehser. (Stahl und Eisen, 1949, vol. 69, Aug. 18, pp. 605–607). An investigation was undertaken to determine whether the formation of titanium or zirconium nitrides in high-speed steel tools would improve their life. Two tool steels (compositions : C 1%, Cr 4%, Mo 2·6%, V 2·7%, W 2·8% ; and C 0·9%, Cr 4%, V 2·6%, W 11·3%) were alloyed with about 0·1% of titanium and 0·1% of nitrogen. In rough-turning tests the life between regrinds when cutting at 12–14 m./min. was increased by 70–80%. This improvement is attributed mainly to improved resistance to tempering.—R. A. R.

Swedish Tool Steel Standards. R. Arpi. (Machinist, 1949, vol. 93, Oct. 15, pp. 879–881). A new schedule for tool steel, comprising 18 standard grades, has been introduced in Sweden. This schedule is discussed and the composition of the steels is shown.—R. F. R.

The Mechanical Properties of Titanium-Alloyed Plates after Cooling in Air from the Rolling Heat. P. Bardenheuer and W. A. Fischer. (Max-Planck-Institut für Eisenforschung : Archiv für das Eisenhüttenwesen, 1949, vol. 20, Sept.–Oct., pp. 313–322). The mechanical properties and microstructure of 0·09% carbon steel alloyed with 0·47%, 0·66%, and 1·24% of titanium, using specimens cut from plates 20 and 12 mm. thick, were investigated. The best mechanical properties at room temperature and the highest creep strength at up to 500° C. were obtained from a heat from an arc furnace with the composition silicon 0·30%, manganese 0·58%, and titanium 0·66%. The commercial production of this plate is difficult and a better understanding of the behaviour of titanium in steel is required.—R. A. R.

Elastic and Fracture Toughness Studies of a Stainless Steel. C. W. Muhlenbruch. (American Society for Testing Materials, 1949, Preprint No. 24). American type 301 stainless steel sheet and rod is shown to have great elastic and fracture toughness, the former being defined as the area under the stress-strain diagram at the yield strength, and the latter as the area under the true-stress/true-strain diagram to the ultimate load. The theoretical fracture toughness thus determined is compared with actual values obtained from tension impact tests. It is shown that the elastic toughness of American type 301 stainless steel is equal to that of high-strength aluminium alloy on a weight-ratio basis and is much higher than that of SAE 1020 or SAE 2315 steel. The theoretical fracture toughness of annealed stainless steel is about three times that of 24S–T aluminium on a weight-ratio basis and three times that of SAE 1020 or SAE 2315 steel. The fracture toughness of these materials as obtained from true-stress/true-strain diagrams is found to agree within 14% with values determined from tension impact tests. The significance of both elastic and fracture toughness is discussed.

Industrial Use of Modified Armour-Plate Steel. A. F. Busick, jun. (Welding Journal, 1949, vol. 28, July, pp. 651–659). The steels in question are a low-alloy steel of 80,000–85,000 lb./sq. in. yield point, 90,000–100,000 lb./sq. in. tensile strength, and 25% elongation, and a high-manganese austenitic steel with chromium, nickel, and molybdenum, having 75,000–100,000 lb./sq. in. yield stress, 115,000–130,000 lb. sq. in. tensile strength, and 35–45% elongation. Uses of these steels in the fabrication of booms and dippers for open-cast coal mining, and the technique of the fabrication of this equipment, including welding, are described.—J. P. S.

Static Properties of Cast Steels. H. A. Schwartz. (Foundry, 1949, vol. 77, July, pp. 70–73, 184, 186). The author attempts to show what values of properties measured by static tests, tensile strength, yield strength, elongation, reduction of area, and Brinell hardness, can be expected simultaneously in cast steels, given unlimited choice as to composition and heat-treatment. Details are given of experimental work with nearly one hundred steels within the following composition range : 0·24–0·52% of carbon ; 0·28–0·89% of silicon ; 0·46–1·70% of manganese ; trace–3·35% of nickel, trace–1·12% of chromium ; trace–0·49% of molybdenum ; trace–0·15% of vanadium ; trace–1·64% of copper ; 0·035% of sulphur ; and 0·035% of phosphorus. The interrelationship of tensile properties is examined to determine if the knowledge of one such property can enable the others to be calculated with reasonable accuracy.—J. C. R.

Nodular Cast Irons. C. More. (Métallurgie, 1949, vol. 81, Sept., pp. 15, 27). A brief note is presented on the development of nodular cast irons.—J. C. R.

Properties of Cast Iron in Relation to the Carbon Equivalent Value. H. T. Angus, F. Dunn, and D. Marles. (American Foundrymen's Society, 1949, Preprint No. 58). The first part of this paper describes briefly the previous attempts to find a relationship between the analysis of cast iron and its mechanical properties. The reasons for using the carbon equivalent value (C.E. = $\frac{1}{3}$ (silicon + phosphorus) + total carbon) as a basic standard are given and the relationship between the tensile strength, transverse strength and C.E. value for 673 commercially-produced test bars of sizes 0·6 in. to 2·1 in. in dia. shows that the correlation is sufficiently good to be of value for unalloyed and uninoculated irons. In the second part of the paper a diagram is produced showing that the structure of a range of sections up to 1·5 in. in dia. can be approximately predicted from the C.E. value using as a basis five characteristic microstructures. In the discussion of the results the structures are referred to, their commercial uses indicating the value of the diagram in the foundry. The effect

of the volume/area ratio on cooling rate and strength, and the limitations of this method of calculation are shown. Figures are given showing the heterogeneity of mechanical properties even in compact sections due to difference in speed of cooling, and the importance of realizing the limitations of the C.E. value is stressed.

Manganese in Cast Irons. A. Esme. (Métallurgie, 1949, vol. 81, Aug., pp. 11, 13, 15, 17). An account is given of the work of various investigators on the carbides of manganese, the deoxidizing effect of manganese, and the effect of the manganese content on the structure and mechanical properties of cast irons.—J. C. R.

Some Applications of Alloy Cast Irons for Internal Combustion Engines. R. A. Chavy. (Métallurgie, 1949, vol. 81, Aug., p. 19). A brief account is presented of the suitability of alloy cast irons for parts of internal combustion engines.

The Production of Crushing Machinery for Cement Works. H. Gernelle. (Fonderie, 1949, Mar., pp. 1521–1524). The use of ordinary and martensitic white cast irons and special cast irons for crushing plant in cement works is discussed. The importance of good wear-resisting properties is emphasized. J. C. R.

Steel Specifications from the Consumers Standpoint. M. L. Frey. (Steel, 1949, vol. 125., Aug. 29, pp. 60–63, 88). The author points out that the present chemical specifications for carbon and low-alloy steels admit of extremely wide variations, particularly in carbon contents, which makes uniform heat-treatment more difficult; many of the grades listed overlap with one another as far as important constituents are concerned. He suggests that there are too many steels, that the consumer can help to remedy this, and that ordering material by hardenability as determined by the Jominy end-quench test, though it does not eliminate the necessity for chemical specifications, can help to provide closer control for uniform behaviour.—J. P. S.

METALLOGRAPHY

Flat-Fielded Reflecting Micro-Objectives. R. L. Drew. (Nature, 1949, vol. 164, Aug. 27, p. 360). The author gives preliminary descriptions of two reflecting micro-objectives which will give flat fields of observation without introducing chromatic focal errors. Both are constructed of fuzed quartz and, of the reflecting surfaces, in each case only one is non-spherical: the reflecting surfaces cannot lose their alignment, the objective being solid. The dry type described has a working distance of 1 cm., a numerical aperture of 0·6 and magnification of 48; the oil-immersion type has a working distance of 2 mm., a numerical aperture of 0·9, and magnification of 10.—J. P. S.

Metallurgy and the Electron Microscope. J. Lomas. (Instrument Practice, 1949, vol. 3, Aug., pp. 457–459). After touching briefly on the working and construction of the electron microscope, the resolution, magnification, focal depth, and the scanning and replica techniques are considered. An electron beam of 0·05 Å. wavelength is used, and with standard units resolutions from 20 to 40 Å. can be made. The limits of particle size for efficient study are approximately ten times the resolving power. The focal depth is at least 5 microns. The scanning method is extremely intricate and expensive and has still a long way to go before it can equal or excel the results obtained with the replica process.—R. A. R.

The Microstructural Characteristics of the Iron–Carbon System. H. Klemm. (Archiv für Metallkunde, 1949, vol. 3, Aug., pp. 265–271). The iron–carbon diagram is shown and the metastable fields and the phases present in each, as well as the actual microstructures at room temperature, are listed and classified. Micrographs of these structures and of those produced by heat-treatment are reproduced and described. R. A. R.

The Microscopical Detection of Temper-Brittleness in Low-Alloy Carbon Steels. P. A. Jacquet. (Comptes Rendus, 1949, vol. 229, Oct. 10, pp. 713–715). The use of picric acid in ether solution, to which is added an appropriate reagent the purpose of which appears to be to increase the wetting of the polished surface, to reveal the grain boundaries in temper-brittle steel is very delicate, and the results are open to question. The author outlines his microscopical observations on two low-alloy steels (one susceptible to temper brittleness, the other not) after polishing electrolytically, and then after etching in various reagents including picric acid or perchloric acid in ether solution without special addition, Benedicks' reagent, the above-mentioned polishing bath at 40° C., as well as standard reagents such as Murakami's reagent, alkaline picrate, etc., and describes the different appearances presented by the specimens.—A. E. C.

A New Method for Determining Austenitic Grain Size of Cast Steel. E. J. Eckel and S. J. Paprocki. (American Foundrymen's Society, 1949, Preprint No. 5). A method is described for determining austenitic grain-size of cast steel, based on the intergranular diffusion of bronze into steel at elevated temperatures. It is claimed that this method gives a clearer microstructure and is therefore easier to evaluate than other methods; it gives satisfactory results for a wide range of steel compositions and austenitizing temperatures; the procedure is the same regardless of the composition or hardenability of the steel.

Metallographic Grain-Size Measurement. I—Technique for Grain-Size Measurements. II—Two New Rapid Processes for Determining the Cross-Section of Grains. H. Kostron. (Archiv für Metallkunde, 1949, vol. 3, June, pp. 193–203; July, pp. 229–242). The discussion in Part I of the principles of grain-size measurement and the accuracy obtainable indicate that the following considerations affect the choice of method: (1) The unavoidable scatter is so great that there is no point in employing methods which require a great deal of time to obtain a very accurate reading; scatter of the order of $\pm$ 10% is acceptable. (2) Methods of determining mean grain-size should be such that the determinations of the coarser grains plays the more important part. (3) To determine the complete grain-size distribution it is best to use a logarithmic scale with an accuracy in proportion to the grain-size; a logarithmic scale is also recommended for ascertaining the mean grain-size. (4) Owing to the limited accuracy it is in most cases sufficient to measure by comparison with a standard series of micrographs.

The first of the methods of grain-size measurement described in Part II is based on making a visual selection of the mean size of the grains in the area under examination and comparing this with a series of standards. The second one is for measuring the size of individual grains by means of a transparency on which is ruled a series of squares of increasing size.—R. A. R.

Grain Size of Martensite after Treatment at Very Low Temperatures. J. Mazur. (Nature, 1949, vol. 164, Aug. 27, pp. 358–359). The author describes experimental work on the measurement of martensite grain-size after quenching successively in brine, liquid nitrogen, and liquid helium. The intense line-broadening found in X-ray diffraction studies of this quenched material may be due to both the smallness of the grain-size and the internal stress in the grains; the author concludes that it is due largely to the latter cause, and that the martensite grain-size is similar to that of the parent austenite.—J. P. S.

Influence of Magnetic Field on Recrystallization. R. Smoluchowski and R. W. Turner. (Journal of Applied Physics, 1949, vol. 20, Aug., pp. 745–746). The influence of a magnetic field on the recrystallization of iron–cobalt alloys is being studied to obtain information on the origin of preferred orientation of recrystallized grains. Preliminary results indicate a change of the texture which can be qualitatively accounted for by considering the magnetostrictive properties of the alloy and their influence on the stability of the recrystallization centres.—R. A. R.

Nomograms for Determining the Exposure Conditions in X-Ray Investigations of Coarse Structures. H. Verse. (Metallwirtschaft, 1949, vol. 3, June, pp. 179–184). After briefly reviewing the known quantitative relationships governing the successful production of X-ray pictures, nomograms to facilitate the determination of current, tube-film distance exposure, and optimum specimen thickness are presented. R. A. R.

An X-Ray Method for Studying Rapid Phase Changes in Steels at High Temperatures. H. T. Heal and J. Savage. (Nature, 1949, vol. 164, July 16, p. 105). The X-ray study of phase changes at high temperature has been simplified by a simple method, relying on the fact that the phases of interest have isolated and unique diffraction angles. With the specimen in the form of a plate, placed in a furnace with its surface parallel to the axis thereof, X-rays are projected at a glancing angle on to the surface and received by a Geiger-Muller counter set to receive the diffraction line from the

low-temperature phase. Through an amplifier, the counter operates a pen recorder, and by recording the rise of temperature simultaneously, the proportion of low-temperature phase present at any moment and the temperature may be determined.—J. P. S.

Mechanism of the γ–α Transformation in Iron. J. Savage. (Metal Treatment and Drop Forging, 1949, vol. 16, Summer Issue, pp. 77–81, 89). This is a review of a paper by H. Neerfeld and K. Mathieu (*see* Journ. I. and S.I., 1949, vol. 163, Sept., p. 114), modified and amplified by Mr. Savage. The original conclusions are substantiated, but in the case of transformations initiated by rolling the material, it is possible that the γ-[111] direction may orientate itself in the rolling direction. This should transform to an α-[110] direction, which would contradict the results obtained in transformation under tension.—J. P. S.

The Graphical Presentation of Quaternary Systems. H. Klemm. (Archiv für Metallkunde, 1949, vol. 3, July, pp. 247–250).

Austenite Transformation in the Hardenable Chromium Stainless Steels. P. S. Spencer and S. W. Poole. (Metalen, 1949, vol. 4, Sept., pp. 1–9). [In English]. The austenite–martensite transformation of high-chromium steels was studied by dilatometer measurements and curves for change in length with temperature, and isothermal transformation diagrams are presented. The sluggish nature of the austenite transformation in the air-hardening stainless grades as contrasted with alloy constructional steels is such that no previous austenite transformation occurs on air-cooling down to the start of the martensite reaction. The austempering of these steels in a salt bath held in the 800–900° F. range is not practical as the time for significant transformation to some intermediate product is much more than 24 hr. Small forgings of the 410, 416, 420, 440 steels (American Iron and Steel Institute types) could be isothermally annealed direct from the forging hammer as the time for transformation is only an hour or so.—R. A. R.

Free Energy and Metastable States in the Iron–Nickel and Iron–Manganese Systems. F. W. Jones and W. I. Pumphrey. (Journal of The Iron and Steel Institute, 1949, vol. 163, Oct., pp. 121–131). A study has been made of the transformations which occur in iron-rich binary iron–nickel and iron–manganese alloys at heating and cooling rates of the order of 10° C./min. The progress of the transformations on continuous heating and cooling has been followed dilatometrically. A quantitative explanation of the transformations is given by the application of thermodynamical principles to the equilibrium diagram. The energy of lattice strain arising from the transformations appears to be insufficient to explain the observed hysteresis between the transformations on heating and cooling.

Further X-Ray Study of the Equilibrium Diagram of the Iron–Nickel System. E. A. Owen and Y. H. Liu. (Journal of The Iron and Steel Institute, 1949, vol. 163, Oct., pp. 132–137). This paper gives an account of a further study of the equilibrium diagram of iron–nickel alloys by the X-ray method. The materials were investigated in powder and in lump form, at room and at elevated temperatures. The effects of contamination and insufficient annealing of the specimens, and the sensitivity of the X-ray method, were investigated. No difference was observed between the results obtained with powder and those obtained with lump material. The equilibrium diagram for the system recorded previously by Owen and Sully is confirmed, in the main.

Some Observations on the Accuracy of Isothermal Diagrams. B. F. Shepherd. (Iron Age, 1949, vol. 164, Aug. 25, pp. 61–66). The author considers that many isothermal transformation curves may be incorrectly drawn in that transformation in the specimens quenched from the austenitic state into a lead bath may take place before the whole of the specimen has reached bath temperature. He has performed experiments on quick-reacting low-alloy steels which prove this point.—J. P. S.

The Thermodynamics of the Iron Alloys. W. Oelsen. (Stahl und Eisen, 1949, vol. 69, July 7, pp. 468–475). A method of presenting constitutional diagrams for binary iron-rich iron alloys is explained. It is based on thermodynamic considerations, in particular, the difference between the specific heats of solution of the alloying element in the α and β phases. If the alloying element dissolves preferentially in the α-phase, the β-field becomes narrower with increasing additions; if it prefers the γ-phase, the γ-field broadens with increasing additions. Several examples of these diagrams are given and they show among other things, the solubility of carbon in α-iron, that of phosphorus in γ-iron, and the equilibrium conditions in the iron–niobium system. R. A. R.

CORROSION

Corrosion Prevention. F. LaQue. (United Nations Scientific Conference on the Conservation and Utilization of Resources, Lake Success, 1949: Canadian Metals and Metallurgical Industries, 1949, vol. 12, Aug., pp. 14–17, 26). The methods of preventing corrosion reviewed in this paper include: (1) Control of environment, in particular the humidity and oxygen; (2) inhibitors; (3) cathodic protection; and (4) improvements in design.—R. A. R.

New Tests on the Local-Cell Activity of Oxide Coatings on Metal Surfaces. F. Tödt. (Archiv für Metallkunde, 1949, vol. 3, Aug., pp. 273–278). Active local cells on metal surfaces are due either to admixtures of more noble metals or to oxide coatings. There are two ways of studying those oxide local-cell effects which affect corrosion phenomena: (*a*) The increase in the solution velocity of iron caused by aeration of the oxide coating can be followed, and (*b*) direct measurements of the current from a cell consisting of an aerated metal surface and a less noble and practically non-polarizing electrode can be made. The two methods, the results of which are mutually supporting, enable a continuous and quantitative picture to be obtained of the local-cell activity between parent metal and oxide coating, and at the same time the oxidation velocity of the metal surface and the thickness of the oxide layer can be determined. The solution velocities of iron and aluminium have been measured and are discussed.—R. A. R.

The Effect of Acid and Neutral Phosphate Solutions on Mild Steel. G. Seelmeyer. (Archiv für Metallkunde, 1949, vol. 3, Aug., pp. 289–294). Phosphate additions to the water in central-heating installations have an immediate effect in reducing corrosion, but, after about 12 or 18 months, corrosion recommences. This phenomenon has been studied by measuring the effects of different quantities of $(PO_4)'''$ irons in distilled water on the iron potential at room temperature and at up to 70° C. Provided the addition of sodium phosphate was less than about 500 mg./l. the potential was instantly less noble, but above this limit it became more noble by amounts which increased with the addition. After a period of some months the phosphate addition nearly always raised the potential. When water containing sodium phosphate is heated iron in contact with it instantly becomes less noble. The amount of the potential increase due to the addition is markedly affected by the *p*H value of the water, and this is caused by the formation of an iron phosphate ($FePO_4$) layer. The layer itself does not inhibit corrosion so it must be assumed that this layer is not permanent and is probably porous. The phosphate coating only prevents corrosion when the *p*H value of the water is adjusted by the addition of an alkali to above 8·8, and by the selection of a suitable phosphate.—R. A. R.

Surface Problems of the Chemical Behaviour of Metallic Materials. W. Guertler. (Metalloberfläche, 1949, vol. 3, July, pp. A-133–A-141). After making general observations on the fundamentals governing chemical behaviour the author considers the differences in the properties of materials as such and those of particular parts made from the material, directing special attention to the relation between the surface and the space lattice. Chemical attack in which material is removed is fundamentally different from that in which material is added. These two kinds of corrosive attack are dealt with in detail with special reference to the manner in which an added alloying element affects the crystalline and atomic structures of the basis metal and gives it greater chemical stability.—R. A. R.

Corrosion Inhibitors for Steel. W. G. Palmer. (Journal of The Iron and Steel Institute, 1949, vol. 163, Dec., pp. 421–431). Many inhibitors, added to water or chloride solution in amounts insufficient to stop attack altogether, produce localized attack, which is more intense than the general corrosion met with in the absence of an inhibitor. Inhibitors which are free from this danger are, in most cases, inefficient. It is believed that the danger is connected with the formation of blister-like membranes over the sensitive points, which prevent the access of the inhibitor to those very points where it is required. By using suitable mixtures of phosphate and

chromate (or phosphate and persulphate), so that any substance precipitated will not be ferrous phosphate but ferric phosphate (a crystalline non-membranous body), the danger can be avoided. Even if the mixture is added in an amount too small to stop corrosion altogether, no intensified corrosion or pitting is observed.

Sodium Nitrite As an Inhibitor against the Attack of Sea Water on Steel. Part I—The Influence of Sodium Nitrite on the Area and Intensity of Attack. Part II—The Addition of Other Inhibitors to Nitrite. D. Wyllie and G. C. N. Cheesman. (Journal of the Society of Chemical Industry, 1949, vol. 68, June, pp. 165–168; July, pp. 209–212). In the presence of concentrations of sodium nitrite which are sufficient to reduce the attack to small intensity, the action of sea water on steel gives rise to a widespread brown coloration, normal pitting ultimately appearing at the centre of each stain. This is considered to be a thin semi-protective film of oxide. A catalytic mechanism for the inhibiting action of nitrites is suggested. Improved results were obtained on mixing phosphates and nitrites. Small amounts of mono- and dihydrogen phosphates added to 5% of sodium nitrite prevented the corrosion of steel when it was the only metal present, without the deposition of any large amount of inhibitor. Sodium hexametaphosphate gives a high degree of protection when only 0·05% is added to 5% of sodium nitrite; adding 0·01% of sodium carbonate increases the efficiency of this mixture. The cathodic action of sodium carbonate as reported by other workers was not observed when 0·05% was added. Sodium sulphite increases the efficiency of nitrite, but owing to the decay of the sulphite the effect is not permanent.—R. A. R.

Marine Boiler Deterioration. I. G. Slater and N. L. Parr. (Proceedings of the Institution of Mechanical Engineers, 1949, vol. 160, No. 3, pp. 341–358). *See* Journ. I. and S.I., 1949, vol. 162, May, p. 126.

Cathodic Protection of Steel in Hot Water by Alloys of Magnesium, Zinc, and Aluminium. R. R. Rogers, and G. E. Viens. (Transactions of the Canadian Institute of Mining and Metallurgy, 1948, vol. 51, pp. 135–142). *See* Journ. I. and S.I., 1948, vol. 158. Jan., p. 157.

Practical Corrosion Control on Gas Transmission Lines. G. R. Olson and H. V. Beezley. (Corrosion, 1949, vol. 5, Aug., pp. 249–253). Data on the costs of installing cathodic protection for buried gas pipes are presented, and recommendations are made on the selection of suitable equipment for particular cases.—R. A. R.

Corrosion of Mild Steel by Sulphuric Acid. H. R. Archer and J. Howlett. (Chemistry and Industry, 1949, Aug. 27, pp. 605–607). The corrosion of mild steel in static and stirred sulphuric acid solutions in strengths within the 65–92% range at temperatures of 60°, 70°, and 80° C. was studied. In static tests attack was initially high in all cases, but after 24 hr. it fell to a comparatively constant figure, except with 85% acid at which a high rate of attack persisted. Corrosion was low and fairly constant in 65, 70, 75, and 92% strengths, and it was only slightly affected by increase in temperature in the 60–80° C. range. With reduction of concentration below 65% there was a rapid increase in the corrosion rate. The dynamic tests showed slightly higher rates of corrosion and confirm the general trend of the static test data. An anomaly was the reduced corrosion rate with increase of temperature using commercial acid diluted to 60%.—R. A. R.

An Unsuitable Application of Chromium Steel. W. Katz. (Metallwirtschaft, 1949, vol. 3, July, pp. 222–223). It is shown that high-chromium steels should not be used in chemical plant for the manufacture of the alkaline chlorides such as common salt, potassium chloride, and magnesium chloride as these soon cause intercrystalline corrosion.—R. A. R.

Results of Some Plant Corrosion Tests of Welded Stainless Steels. G. F. Comstock. (American Society for Testing Materials, 1949, Preprint No. 23). Corrosion tests of welded stainless steels were made in a number of chemical plants under regular operating conditions, and the results are compared with those obtained on the same steels tested by the standard boiling nitric acid procedure in the laboratory. The steel specimens included the American types 304, 321, and 347 sheets, welded with types 308, 321, and 347 electrodes respectively, and tested both in the as-welded and stress-relieved conditions. Results showed no correlation between the standard nitric acid test and the plant corrosion tests.

The Pulse Polarizer in Corrosion Technology. H. J. McDonald and G. A. Marsh. (Corrosion, 1949, vol. 5, Aug., pp. 254–260). The pulse polarizer consists essentially of a high-voltage D.C. source, a pulse discharge mechanism, a sensitive polarization detector and a high-speed strip-chart recorder. A high-voltage condenser is discharged almost instantaneously across a cell made up of a solution containing two electrodes, and the potential of the electrode (the metal specimen, the corrosion rate of which is being studied) is measured against a standard reference calomel electrode which is also immersed in the solution. The potential difference between these electrodes is detected on an electronic voltmeter circuit, amplified, and applied to the recorder which traces the potential of the test electrode as a function of time. The curves are reproducible and sensitive to small changes in conditions. The applications of the instrument to the study of corrosion inhibitors, to the choice of a metal for use in a particular environment, to the identification of alloys, and to the selection of an inhibitor for stress corrosion are described.—R. A. R.

Application of Alloy Steel Linings to Tanks Storing Corrosive Pulp Cooking Liquor. H. A. Schmitz and M. A. Scheil. (Corrosion, 1949, vol. 5, Aug., p. 271). Tests have been carried out with stainless steels to find a material for storing sulphite liquor at normal temperature and pressure and at high temperature and 45 lb./sq. in. For the latter conditions stainless steel 316 (Cr 16–18%, Ni 10–14%, Mo 2–3%) and for the former stainless steel 347 (Cr 17–19%, Ni 9–12% plus niobium) were found most suitable.—R. A. R.

The Resistance of High-Alloy Steels to Industrial Atmospheres. H. T. Shirley. (British Science News, 1949, vol. 2, No. 22, pp. 294–298). Specimens of 22 steels and five non-ferrous materials have been exposed to the Sheffield atmosphere for periods of two and five years and the loss in weight has been determined. One mild steel, one low-alloy, and 20 high-alloy steels were used. An addition of 2·5% of molybdenum to 18/8 stainless steel greatly increased the corrosion resistance and a 24/12/3 chromium–nickel–wolfram steel was also surprisingly resistant. The high-speed and the low-alloy nickel–chromium–molybdenum steels had weight losses about 20% of that of mild steel. Taking the corrosion rate of mild steel over five years as 100, the rates in the first month and the first year were 357 and 167 respectively.—R. A. R.

Gas–Metal Reactions. A. Dravnieks, and H. J. McDonald. (Iron Age, 1949, vol. 164, Aug. 25, pp. 78–82; Sept. 1, pp. 84–86). The authors discuss the nature of gas–metal reactions at high temperatures and examine the various theories of scale formation at present under consideration: They note that changes of phase with temperature, in either scale or metal, may influence the rate of growth, observing that, of the three phases FeO, Fe_3O_4 and Fe_2O_3, the last two are less permeable to the diffusion of iron atoms than is FeO, on account of their lattice differences. They next consider experimental methods of studying scales, examining the X-ray, electron-diffraction, radio-active tracer, conductivity, micro-balance, and gas-volumetric methods. The last two are considered the most universal and versatile.—J. P. S.

The Relationship between Adsorption Capacity and Corrosion. G. F. Hüttig. (Berg- und Hüttenmännische Monatshefte der Montanistische Hochschule in Leoben, 1949, vol. 94, Aug.–Sept., pp. 282–284). If an adsorption isotherm for a metal powder and a gas is constructed, one can calculate, using the Langmuir equation, a value x which is proportional to the surface area and a value K which is proportional to the surface quality. This is the Grubitsch relationship, the validity of which was checked, using carbonyl iron powder heated to different temperatures in hydrogen, cooled, and allowed to adsorb CH_3OH vapour.—R. A. R.

Surface Reactions of Metals. G. Tolley. (Metal Industry, 1949, vol. 75, July 22, pp. 68–70; July 29, pp. 89–91). The first part of the paper describes methods of measuring the surface roughness and surface area of a metal. In the second part the relation between surface condition and corrosion and oxidation are dealt with. The more rapid initial fall of potential at a coarsely ground surface on immersion in 0·5M potassium sulphate solution is explained by the greater rate of chemical reaction occasioned by a more uneven oxide film than is present on a smoother surface. The author inclines to T. P. Hoar's view that the adsorption of organic inhibitors occurs at anodic areas. Four types of oxidation law are known; these express rectilinear, parabolic, logarithmic, and asymptotic relationships between the weight increment with time. The parabolic curve is the 'model' law

based on diffusion across the oxide barrier, and Wagner's theory that the electrical resistance of the film to the passage of ions is the main factor deciding the rate of oxidation gives a complete demonstration of the mechanism causing it.—R. A. R.

Causes of Flue Gas Deposits and Corrosion in Modern Boiler Plants. W. F. Harlow. (Proceedings of the Institution of Mechanical Engineers, 1949, vol. 160, No. 3, pp. 359–379). *See* Journ. I. and S.I., 1949, vol. 163, Nov., p. 364.

ANALYSIS

Determination of Metallic Iron as well as the Ferrous and Ferric Oxides. H. U. von Vogel. (Archiv für das Eisenhüttenwesen, 1949, vol. 20, Sept.–Oct., pp. 287–292). The methods given in the literature for determining metallic iron and the ferrous and ferric oxides in iron ores are critically examined. The copper sulphate method of Sims and Larsen and that of Bohm are found to be insufficiently accurate and too complicated. Trials with organic solvents for the bromine method led to a new bromine–alcohol method which proved satisfactory for routine work. More than 6% of manganese interfered. The crushing of ore in a steel mortar or an iron ball mill may cause the metallic iron determination to be 0·1–0·2% above the true value.—R. A. R.

Localized Spectrum Analysis of Low-Alloy Steel. N. G. Isaev. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 1010–1011). [In Russian]. A circuit and apparatus for the production of a localized excited zone on the specimen of low-alloy steel undergoing spectrum analysis is described, and the calibration curve obtained for the determination of chromium is shown.—S. K.

Rapid Determination of Ferrous Oxide in Sinter. N. Z. Plotkin, Iu. I. Usatenko and P. A. Bulakhova. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 999–1000). [In Russian]. The determination of ferrous oxide in sinter is carried out, in the method described, by finding the gain in weight of a finely ground sample after heating in a stream of air or oxygen at 1100° C. Preliminary experiments show that under these conditions complete oxidation of FeO to Fe_2O_3 occurs within 3 min. By finding the weight of carbon dioxide formed during the oxidation, the determination of carbon in the sinter is simultaneously carried out. The results were found to agree well with those obtained by the normal volumetric method. It was found that fayalitic FeO remained unoxidized at lower temperatures, and it is suggested that such FeO can be determined by carrying out the oxidation at two temperatures.—S. K.

Investigation of the Effect of Sparking and of the Influence of Third Elements during Spectrum Analysis. L. N. Filimonov. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 919–936). [In Russian]. This paper represents the first contribution to a study of the influence on the relative intensities of spectrum lines, of the action of the sparking on the specimen electrode, and of the effect on these intensities of the presence of other elements. Discussion of these phenomena is followed by a comprehensive survey of the literature, and this leads to the selection of methods for their experimental investigation. A condensed spark was used for excitation, and in the first experiments the steel electrodes were sparked in controlled oxidizing atmospheres inside a specially designed vessel provided with a quartz window. The atmospheres used were: nitrogen containing 0·3% of oxygen; air; and oxygen containing 0·5% of nitrogen. Curves for intensity against time for silicon, manganese, copper, chromium, and aluminium were obtained for periods of about 20 min. after sparking started. It was found that the effect of changes in the concentration of oxygen in the atmosphere on the shape of these curves increased with increasing self-induction of the discharge circuit. The appearance of the electrodes after 10 min. sparking in the various oxidizing atmospheres is illustrated. Curves were similarly obtained for silicon and for copper in reducing atmospheres. To obtain data for the first few minutes of sparking, the sparked surface was increased by using a rotating disc as electrode, and the intensity-time curves obtained by this method for various steels are shown. Processes taking place on the sparked surface of the electrode are discussed in terms of the results obtained in the above experiments.—S. K.

Study of the Distribution of Elements among the Phases of an Alloy. Influence of Cementite on the Relative Intensities of the Iron Lines. H. Triché. (Comptes Rendus, 1949, vol. 229, Oct. 3, pp. 652–653). The study of the distribution of the elements in an alloy by spectrography can be made difficult as the result of the action of the source on one constituent. In iron–carbon alloys the accentuation of the majority of the iron lines is due to a thermal effect of the source on the cementite. This phenomenon contributes to some extent to the fatigue of the spark.—A. E. C.

Fast Analysis of Acid Slags. Reflective Spectrophotometry Determines FeO–MnO–Cr. E. C. Zuppann and A. E. Martin. (American Foundrymen's Society, 1949, Preprint No. 30: American Foundryman, 1949, vol. 15, Apr., pp. 126–132). A method is described for the determination of ferrous oxide, manganese oxide, and chromium in acid slags. A finely ground slag sample is mixed with oil and examined in the spectrophotometer at a few key wavelengths. The reflective densities determined are translated into percentages of ferrous oxide, manganese oxide, and chromium by reference to graphs. It is suggested that this method would be of use in the control of acid steelmaking processes.—J. C. R.

Spectrographic Analysis of Slags Using Spark Technique. J. Eeckhout. (Spectrochimica Acta, 1949, vol. 3, No. 5–6, pp. 575–583). A method for the quantitative spectrographic analysis of slags is described. A high-voltage condensed-spark excitation is used with powdered samples on a flat pure graphite electrode. The method gives sufficient accuracy for slag control and the results obtained for several elements were more reliable than those of routine chemical analysis. R. A. R.

On the Question of the Spectrum Analysis of Basic Open Hearth Slags. O. I. Nikitina. (Zavodskaya Laboratoriya, 1949, vol. 15, July, pp. 855–857). [In Russian]. Experiments are described on the spectrum analysis of basic open-hearth slags, using nickel electrodes and arc excitation. Results obtained are compared with those of chemical analysis.—S. K.

Rapid Method for the Determination of Calcium Carbide in Slag. A. G. Bogdanchenko. (Zavodskaya Laboratoriya, 1949, vol. 15, July, pp. 851–853). [In Russian]. The calcium carbide in slag is determined, in the method described, by treating the sample with dilute acid, oxidizing the acetylene produced by passing through a heated tube with oxygen, and determining the carbon dioxide.—S. K.

The Estimation of Oxygen in Liquid Steel. Application to Basic Open-Hearth Steel. M. Lacomble and M. Nepper. (Société Française de Métallurgie, Oct. 19, 1948: Revue de Métallurgie, Mémoires, 1949, vol. 46, Aug., pp. 561–565). The authors take their samples of molten steel in an aluminium-charged bomb; this is a cube externally, in two parts, with the internal spherical space machined out. The alumina formed by reaction with the oxygen in the steel is measured nephelometrically. The authors also discuss the influence of the carbon content of the steel and the basicity of the slag on the oxygen content of the steel.—A. E. C.

Study of the Perchloric Method for Estimating Chromium and Vanadium in Steels. B. E. Jaboulay. (Revue de Métallurgie, Mémoires, 1949, vol. 46, Oct., pp. 710–712). The author reviews the opinions of other workers on the perchloric acid method of estimating chromium and vanadium in steels, and then describes his own method and the precautions taken to avoid erroneous results.—A. E. C.

Photocolorimetric Analysis of Stainless Steels. K. A. Shisterman and O. A. Yakovleva. (Zavodskaya Laboratoriya, 1949, vol. 15, July, pp. 782–785). [In Russian]. Details are given of the photocolorimetric determination of silicon, nickel, titanium, and molybdenum in stainless steels. For the determination of silicon the usual photocolorimetric procedure was modified as regards solution of the sample and calibration. For the determination of nickel, the sample was dissolved in sulphuric acid and the dimethyl-glyoxime complex was used. The determination of titanium was based on the formation of a yellow colour during the interaction of this metal in sulphuric acid with hydrogen peroxide. For the determination of molybdenum, the normal photocolorimetric procedure, modified to deal with molybdenum contents up to 5% in the presence of 16–19% chromium and 12–14% nickel, was used. The above determinations are said to be sufficiently rapid for control purposes.—S. K.

Electrographic Analysis for Identifying High Temperature Alloys. M. E. Levy. (Iron Age, 1949, vol. 164, Aug., pp. 98–100). By making the specimen the anode in a simple electrolytic cell, the electrolyte being a specific reagent absorbed on a filter paper, constituents may be qualitatively identified. Typical observations are as follows: with chromium,

where the specific reagent is 5% potassium nitrate with acetic acid–benzidine solution, an intense dark-green colour is given, changing slowly to blue-black; with tungsten, the reagent is 5% hydrochloric acid plus a 0·01% solution of Rhodamine B, and a purple colour is given. Nickel, cobalt, molybdenum, titanium, and iron may be identified equally easily, but niobium does not give a specific reaction; a very high cobalt content may mask a small nickel content, and molybdenum in quantity may give a false 'tungsten' indication.—J. P. S.

Determination of Carbon—Simplification of Low Pressure Combustion Apparatus. W. M. Murray and L. W. Neidrach. (Industrial and Engineering Chemistry (Analytical Edition) 1944, vol. 16, Oct., pp. 634–636). The interference of water vapour in the apparatus for the low-pressure combustion method of determining carbon in iron and steel has been investigated, and changes in operating technique have minimized this interference. The equipment has been simplified further, resulting in a greater speed of manipulation. In over 600 determinations made with the new equipment the results have shown good agreement with those obtained on earlier forms of apparatus.

Routine Determination of Nickel in Cobalt-Base Alloys. L. Silvermann and H. K. Lembersky. (Analytical Chemistry, 1949, vol. 21, Aug., pp. 983–984). Cobalt salts in ammoniacal citrate solution are oxidized to the trivalent state with potassium ferricyanide solution, and the nickel precipitated with dimethylglyoxime. Tungsten, molybdenum, chromium, iron, manganese, niobium, and titanium do not interfere, and one part of nickel may be precipitated in the presence of 200 of cobalt.—J. P. S.

Spectrographic Determination of Phosphorus in Pig Iron. M. S. Alpatov. (Zavodskaya Laboratoriya, 1949, vol. 15, July, pp. 857–858). [In Russian]. Data are presented relating to the accuracy of spectrographic determinations of phosphorus in pig iron samples containing 0·06%, 0·09%, and 0·5% of this element.—S. K.

Spectrographic Determination of Magnesium in Cast Iron. W. R. Kennedy. (Foundry, 1949, vol. 77, Oct., pp. 80–81, 182). Three methods for the determination of magnesium in cast iron are described: (1) Wet chemical analysis; (2) spectrographic rod or pin method; and (3) the flat surface spectrographic method.—J. C. R.

The Determination of Chromium in Cast Iron and Steel. T. S. Harrison and H. Storr. (Analyst, 1949, vol. 74, Sept., pp. 502–504). The procedure followed in determining the chromium content in graphitic cast iron and other ferrous alloys using potassium bromate as an oxidizing agent, is described and some experimental results are given.—J. C. R.

Rapid Method for the Determination of Total Sulphur in Coals and Coke. A. T. Chernyi and K. V. Podoinikova. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 1002–1003). [In Russian]. In the method described, total sulphur in coal and coke is determined by fusion at 750–800° C. with metallic calcium and dry oxalic acid in a stream of carbon dioxide. The hydrogen sulphide evolved is absorbed in solutions of the acetates of zinc and cadmium and determined iodometrically.—S. K.

Rapid Method for the Determination of Combustible Sulphur in Coal, Anthracite and Coke. M. M. Lifshits and O. A. Minenko. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 1000–1002). [In Russian]. Results obtained by various combustion methods for the determination of combustible sulphur in solid fuels are compared with those obtained by the fusion method. An improved procedure for the combustion of the samples and the estimation of the sulphur oxides produced is described.—S. K.

Notes on Installation and Operation of Automatic Instruments for Analysis of Combustion Flue-Gases. A. J. Potten and G. C. H. Sharpe. (Journal of the Institute of Fuel, 1949, vol. 22, Aug., pp. 307–312, 321). Modifications to automatic instruments for flue-gas analysis to render them able to follow, as quickly and accurately as possible, frequent and wide fluctuations which may occur in experimental combustion equipment are described. The modifications are mainly the result of experience with one instrument, the Monc, a description of which is given. It is also shown how to enable this instrument to measure gases outside its normal scope.—R. A. R.

Spectral Method for the Analysis of Certain Ferro-Alloys for Silicon and Chromium. P. D. Korzh and A. V. Kozlova. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 937–939). [In Russian]. Experiments are described in which the visual spectroscopic method of P. D. Korzh was applied to the determination of silicon in specimens of ferrosilicon, silicomanganese, and ferromanganese containing 9·9%, 12·5%, and 1·08% of silicon, respectively, and of chromium in specimens of ferrochromium, and of silicochromium with 57·1% and 29·8% of chromium, respectively. Results obtained are compared with those of chemical analyses, and the influence on the results of the time for which a given specimen has been exposed to the discharge are considered.—S. K.

Spectral Analysis of Ferrosilicon. I. M. Veselovskaya. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 940–944). [In Russian]. The results are presented of experiments in which the author's recently described technique of slag analysis using briquetted samples was satisfactorily applied to the spectral analysis of ferrosilicon. The samples of the ferroalloy of various compositions up to 75% of silicon, were finely powdered, mixed with copper powder, and briquetted under pressure. The determination of aluminium and manganese was also carried out. Excitation was produced with a condensed spark. The method has also been applied to the analysis of silicochromium.—S. K.

Determination of Tungsten in Ferrotungsten and High-Speed Steels from the Specific Gravity. S. I. Malov. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., pp. 997–998). [In Russian]. Calculated and observed values of the specific gravities of alloys of iron with up to 81% of tungsten are tabulated, and it is shown that the measurement of this property can be used for the determination of tungsten in cases where speed rather than great accuracy is required. S. K.

The Determination of Ferrous Iron in Chromite and Chrome-Magnesite Refractories. S. T. Baliuk and V. M. Mirakyan. (Zavodskaya Laboratoriya, 1949, vol. 15, Aug., p. 1004). [In Russian]. The use of an acid mixture which has been previously treated with permanganate is recommended for the determination of ferrous iron in chromite and chrome-magnesite refractories.—S. K.

Chemical Analysis of Sillimanite, Kyanite, and Related Aluminium Silicate Refractories. H. R. Shell. (Bulletin of the American Ceramic Society, 1949, vol. 28, Sept. 15, pp. 349–352). Aluminium silicate refractories such as mullite, sillimanite, and kyanite are analysed chemically by fusion with CaO plus Na_2CO_3, double dehydration with HCl and $HClO_4$, and determination of Al_2O_3, Fe_3O_4, and TiO_2 in the SiO_2 filtrate. Al_2O_3 is obtained with the reagent 8-hydroxyquinoline, Fe_2O_3 with *o*-phenanthroline, and TiO_2 with H_2O_2. All the aluminium silicate refractories tested were completely decomposed by this method. Indicated accuracy is $\pm$ 0·1% for both SiO_2 and Al_2O_3. Time required by this method is less than for methods now in common use.

Determination of the Excess of Phosphates in Boiler Water with the Aid of Cation Absorption. S. A. Edelshtein and V. I. Petatskii. (Zavodskaya Laboratoriya, 1949, vol. 15, July, pp. 850–851). [In Russian]. In the method described, the determination of the excess of phosphate in the sample of boiler water is carried out by passing a filtered portion through a cation-absorbing filter, determining the total phosphate in the filtrate and determining the hardness, after neutralization, of the acid extract of the filter.—S. K.

ECONOMICS AND STATISTICS

The World Production of Iron in 1948. R. Durrer. (Von Roll Werkzeitung, 1949, vol. 20, Sept., pp. 185–188). Statistics on the production of pig iron and crude steel in 1948 by the different countries of the world are presented and compared with those of previous years. With productions in 1948 of 108 and 153 million tons of iron and steel respectively, the maximum quantities produced in any year up to 1939 have been attained. The United States has increased its proportion of the total from about one-third to one-half.—R. A. R.

The Reconstruction of the Iron and Steel Industry in the Russian-Occupied Zone. M. H. Kraemer. (Die Technik, 1949, vol. 4, Sept., pp. 421–425). The economic position of the iron and steel industry in the Russian-occupied zone of Germany is reviewed. Pig-iron production in 1946, 1947, and the planned production for 1948 were 123,000 tons, 131,000 tons, and 209,000 tons respectively. The corresponding figures for rolled steel were 76,000, 92,000, and 154,000 tons. For 1950 the planned production is 360,000 tons of pig iron and 695,000 tons of rolled steel.—R. A. R.

What's Left of Japan's Iron and Steel Industry? J. Z. Reday. (Steel, 1949, vol. 125, Aug. 8, pp. 68–72, 94, 96). The Japanese steel industry before the war had an ingot capacity of about 8 million tons, and was dependent largely on overseas raw materials, from which it was cut off during the 1939–45 war. Proved reserves of iron ore are low and of poor quality coal, which, though adequate in quantity is not of good coking quality. The ores necessary for ferro-alloy manufacture are not available in Japan. Limestone and dolomite were in adequate local supply, but magnesite for ramming and for refractory manufacture was imported. The author thinks that if all questions of reparations were abandoned and the industry allowed to return to a basis of export sale, it might help to ease world steel shortages.
J. P. S.

MISCELLANEOUS

The Traffic of Iron and Steel Works—A Method of Traffic Analysis. H. H. Marden and M. D. Brisby. (Journal of The Iron and Steel Institute, 1949, vol. 163, Dec., pp. 433–448). A method of traffic analysis applicable to conditions prevailing in iron and steel works is developed. Based on the speed of wagon turn-round, a manner of gauging the general efficiency of a works traffic system is suggested. Considerations on traffic flow and density serve to clarify the problem of transport into, through, and out of, the works, and point to the necessity for co-ordinating rail traffic and the mechanical handling of materials. An investigation into locomotive utilization by means of time studies is developed, showing the necessity for a complete understanding of traffic requirements and locomotive operation for speeding-up transport and improving layout.

Atlantic Steel Company Applies Unit-Load Principle. L. W. Bennett. (Iron and Steel Engineer, 1949, vol. 26, Aug., pp. 108, 110). An account is given of the use of electrically operated trucks at the Atlanta works of the Atlantic Steel Company. The unit-load principle has been adopted and a wide variety of materials ranging from ingots to coils of wire can be handled.—J. C. R.

Packaging, Marking, and Loading Methods for Steel Products for Commercial Overseas Shipments. (United States Department of Commerce, 1949, Simplified Practice Recommendation R237–49).

How to Identify Engineering Materials. B. Melnitsky. (Steel, 1949, vol. 125, Aug. 29, pp. 66–70, 72). To avoid wasted time and material, stock should be clearly marked from the moment of its arrival. A colour code within the factory is very valuable, and two-colour combinations of nine basic colours enable 81 different materials to be distinguished; bars may be painted lengthways and any extra information stencilled on. Where steels to hardenability specifications are involved, colour alone is inadequate, and stencils, tags, or stamped markings are also desirable.—J. P. S.

The Problems and Technique of Standardisation: Statistical Methods in Industry. R. L. Brown. (Monthly Bulletin of the British Coal Utilisation Research Association, 1949, vol. 13, Aug., pp. 261–268). The application of statistical methods to industry with special reference to the development and use of standards is reviewed. The bibliography contains 18 references, and other selected references are made to statistical techniques under classified headings.—R. F. F.

Problems in the Treatment of Cooling Water in Industrial Plants. L. D. Betz and J. J. Maguire. (Blast Furnace and Steel Plant, 1949, vol. 37, Aug., pp. 972–976). Very large volumes of water are used in many industrial processes, and it is usually necessary to circulate this. In systems where this process leads to concentration, scale-forming substances, other than calcium carbonate, which is the primary problem, may precipitate; such substances are calcium and magnesium silicates and calcium sulphate. Acid treatment of the circulating water or base-exchange softening of the make-up water is advised. Control of slimes and algal growth by bactericides or bacteriostatic agents is recommended.—J. P. S.

The Activities of the Verein Deutscher Eisenhüttenleute in the Year 1948. (Stahl und Eisen, 1949, vol. 69, June 23, pp. 431–443).

The Organisation of Research in the French Iron and Steel Industry. G. Delbart. (Mémoires de la Société des Ingénieurs Civils de France, 1948, vol. 101, Mar.–Apr., pp. 251–261). This survey of iron and steel research in France is mainly devoted to the formation of the Institut de Recherches de la Sidérurgie Française (IRSID), with notes on its committees and the directions in which research work will be carried out.—J. C. R.

Metallurgical Teaching and Research. G. Chaudron. (Revue de Métallurgie, Mémoires, 1949, vol. 46, Oct., pp. 645–652). In his inaugural lecture of the course in applied chemistry at the Faculty of Sciences, Paris, May 2, 1949, the author discusses the teaching of metallurgy in the University, and the organization of research in industry, both in the works and in research associations.—A. E. C.

Efficiency Calculation by the Bedeaux System. L. Mayre. (Hutnik, 1949, vol. 16, May–June, pp. 191–204). [In Polish]. The author discusses the Bedeaux system for calculating the efficiency of production. Many examples are given and compared with the Halsey system relating to productivity and wages.—W. J. W.

The Fundamentals of Calculating Planned and Average Costs—The Problem of Standardizing Costing. A. Müller. (Stahl und Eisen, 1949, vol. 69, Aug. 18, pp 601–605).

Age Distribution of the Personnel in the Iron and Steel Industry. H. Euler. (Stahl und Eisen, 1949, vol. 69, Sept. 29, pp. 702–706).

Large-Scale Production of Oxygen. J. Wucherer. (Iron and Coal Trades Review, 1949, vol. 159, Sept. 23, pp. 723–730). The author compares various oxygen-tonnage processes and outlines the fundamentals of plant design, stressing the important progress made in regenerators and referring to their peculiarities. The Linde–Fränkl process is discussed, followed by descriptions of some processes developed recently in U.S.A., Great Britain, and France. Power consumption and capital cost are compared.—J. C. R.

BOOK NOTICES

HOWARD, A. J. "*Canning Technology.*" Pp. vii + 287. Illustrated. London, 1949: J. and A. Churchill, Ltd. (Price 30*s.*)

This is more than a book on canning technology; it deals also with the scientific principles on which the canning industry is based and thus provides stimulating reading for the chemist in general and for research workers on food problems, tinplate, and corrosion phenomena in particular. Mr. Howard has had just the right kind of experience to be well equipped to write a book on this subject, having been employed on the scientific staff of a famous food manufacturing company, and having served a term as Director of Canning to the Ministry of Food in London. Therefore, what he writes is written with confidence and some degree of authority. But it is impossible to read any chapter without being impressed not only with what he says but with how he says it. For clarity of expression this book is outstanding; even in discussions of corrosion phenomena, a subject which seems sometimes to invite the garrulous or nebulous style, Mr. Howard's gift of clear exposition enables him to explain what goes on inside a food can in language which can easily be assimilated.

After a brief résumé of the processes of manufacture of tinplate, chapters follow on the all-important corrosion phenomena inside and outside the can and on the subject of the manufacture of cans. The rest of the book deals with canning technology proper and discusses general considerations in establishing a cannery, examination and preparation of food for canning, heat sterilization, the details of the canning process, the metal content of canned foods, spoilage and its prevention and, finally, recent development.

All who are interested in food canning and food containers will enjoy reading this book and will profit by it.
E. S. HEDGES.

LILLICO, J. W. "*Blacksmith's Manual Illustrated.*" 8vo, pp viii + 211. Illustrated. Kingston Hill, Surrey, 1949: The Technical Press, Ltd. (Price 15*s.*)

This is the third impression of a book first published in 1930. The author demonstrates by drawings and simple text matter, specimens of smith work that are commonly required and the best, simplest, and quickest way to do them.

The book commences with descriptions of the various kinds of forges and hearths that are used by blacksmiths

and then goes on to describe and illustrate anvils, hammers, tongs, and the various types of hand tools and anvil tools that are used in the art. The latter half of the book is devoted to brief but very well illustrated descriptions of the *modus operandi* for producing a number of miscellaneous forgings.

This is a particularly practical book and should be invaluable to a blacksmith.—J. FERDINAND KAYSER.

PANSERI, C. "*Manuale di Fonderia d'Alluminio.*" 8vo, pp. xv + 476, with coloured frontispiece, 130 figures, and 105 tables, some with diagrams. Milan, 1949: Ulrico Hoepli.

After a short historical introduction dealing with the origins of metallurgy, the subject of aluminium is approached, the author referring to the historical importance of the first aluminium casting of artistic interest—the statue of Eros in Piccadilly. A statistical survey follows showing the remarkable increase in production during this century.

The author gives a useful and detailed review of the properties of the metal as it comes into commerce, the primary properties being given in the form of photographic copies of the appropriate Italian specifications, the degree of reduction being such that most readers will find a magnifying glass necessary. After a discussion of the methods of obtaining sound castings, there are chapters on heat-treatment, physical and mechanical properties, and surface protection. Testing and metallography are considered, with a small number of good photomicrographs. Other chapters deal with foundry equipment, including machine tools, as well as electrolytic cleaning and oxidation. There are many tables at the end of the book, and readers of Italian should find the work valuable for reference.—C. H. DESCH.

"*Progress in Metal Physics 1.*" Editor: Bruce Chalmers. 8vo, pp. viii + 401. Illustrated. London, 1949: Butterworth's Scientific Publications. (Price 45*s.*)

It is the purpose of this volume, which is to be the first of an annual series, to present authoritative reviews of the state of knowledge in specialized aspects of the field that includes both physical metallurgy and metal physics. The volume consists of seven sections covering progress of the theory of alloys, theory of dislocations, crystal boundaries, age-hardening of metals, hardening response of steels, preferred orientation in non-ferrous metals, and diffusion of metals in metals.

Each has been written by an acknowledged expert and all of them are most useful. The sections are not devoted to the research work and findings of their respective authors but are adequate reviews of the subject in general; they are well written and arranged, and conclude with an adequate list of references. In view of the number of authorities whose work has gone to make up this book, the price of 45*s.* is not excessive.—J. FERDINAND KAYSER.

NEW PUBLICATIONS

AMERICAN INSTITUTE OF MINING AND METALLURGICAL ENGINEERS. "*Electric Furnace Steel Conference Proceedings.*" Volume 6. Pittsburgh Meeting, December 2–4, 1948. 8vo, pp. 240. Illustrated. New York, 1949: The Institute.

AMERICAN SOCIETY FOR TESTING MATERIALS. "*Metal Cleaning, Bibliographical Abstracts.*" Prepared by J. C. Harris. Special Technical Publication No. 90. 8vo, pp. 68. Philadelphia, 1949: The Society. (Price $2·75)

BERZELIUS, J. J. "*Minnesfesten över Berzelius,* 20–22 September, 1948." (K. Svenska Vetenskapsakademiens Årsbok för År 1948). 8vo, pp. 56. Stockholm: Almqvist och Wiksells Boktryckeri A.B.

BRITISH IRON AND STEEL RESEARCH ASSOCIATION. "*Conference on Cupola and Converter Refractories.*" Ashorne Hill, Leamington Spa, Thursday and Friday, 12 and 13 May, 1949. La. 8vo, pp. 52. Illustrated. London, 1949: The Association. (Price 10*s.*)

BRITISH STANDARDS INSTITUTION. B.S. 1121: 1940. "*Methods for the Analysis of Iron and Steel.*" Part 12: "*Silicon in Acid-Resisting High Silicon Iron.*" Pp. 6. (Price 1*s.*); Part 13: "*Chromium in Iron and Steel.*" Pp. 7. (Price 1*s.*); Part 14: "*Copper in Carbon and Low-Alloy Steels.*" Pp. 6. (Price 1*s.*). London: The Institution.

BRITISH STANDARDS INSTITUTION. B.S. Code 1500: 1949. Provisional. "*Fusion-Welded Pressure Vessels for Use in the Chemical and Allied Industries.*" 4to, pp. 167. London: The Institution. (Price 25*s.*)

BRITISH STANDARDS INSTITUTION. B.S. 1553: Part 1: 1949. "*Graphical Symbols for Pipes and Valves.*" 8vo, pp. 11. London: The Institution. (Price 2*s.*)

BRITISH STANDARDS INSTITUTION. B.S. 1588: 1949. "*Thermal Insulating Materials Suitable for Use within the Temperature Range* 200°*F. to* 450°*F.*" 8vo, pp. 35. London: The Institution. (Price 4*s.*)

BRITISH STANDARDS INSTITUTION. B.S. 1591: 1949. "*Acid Resisting High Silicon Iron Castings.*" 8vo, pp. 7. London: The In titution. (Price 2*s.*)

BRITISH STANDARDS INSTITUTION. B.S. 1613: 1949. "*Resolving Power of Lenses for Cameras and Enlargers.*" 8vo. pp. 11. London: The Institution. (Price 2*s.*)

BROOKE, E. H. "*Appendix to Chronology of the Tinplate Works of Great Britain,* 1665–1949." 8vo, pp. 179–241. Cardiff, 1949: William Lewis (Printers) Ltd. (Price 5*s.*)

CLAPP, W. H. "*Engineering Materials and Processes.*" Second Edition. 8vo, pp. 526. Scranton Pa: International Textbook Co. (Price $6.50)

JESSOP, H. T. and F. C. HARRIS. "*Photoelasticity Principles and Methods.*" La. 8vo, pp. viii + 184. Illustrated. London, 1949: Cleaver-Hume Press, Ltd. (Price 28*s.*)

KUBASTA, J. "*Das Härtungsverhalten der Edelstähle.*" Versuch der zahlenmässigen Erfassung und technischen Auswertung der Härteeigenschaften moderner Edelstähle. 2 Aufl. 8vo, pp. v + 204. Illustrated. Halle (Saale), 1949: Wilhelm Knapp. (Price DM 12.50)

MINISTRY OF LABOUR AND NATIONAL SERVICE. JOINT STANDING COMMITTEE ON CONDITIONS IN IRON FOUNDRIES. Report No. J.S.C./4/1. "*Organic Bonding Materials for Foundry Cores and Moulds.*" A Survey of the Literature on Gases, Vapours and Fumes Evolved Therefrom. F'scap. pp. 38. [Mimeographed]. London, 1949: Ministry of Labour and National Service.

MINISTRY OF SUPPLY. "*Report of the Committee for Standardization of Engineering Products.*" 8vo, pp. 35. London, 1949: H.M. Stationery Office. (Price 9*d.*)

MUREX WELDING PROCESSES, LTD. "*Handbook for Electric Welders.*" Tenth Edition. 8vo, pp. 190. Illustrated. Waltham Cross, 1948: The Company. (Price 7*s.* 6*d.*)

NIEZOLDI, O. "*Ausgewählte chemische Untersuchungsmethoden für die Stahl- und Eisenindustrie.*" 4 Aufl. pp. vii + 184. Berlin, Göttingen, Heidelberg 1949: Springer-Verlag. (Price DM 9.60)

PARTRIDGE, J. H. "*Glass-to-Metal Seals.*" 8vo, pp. xii + 238. Illustrated. Sheffield, 1949: Society of Glass Technology. (Price 35*s.*)

PIWOWARSKY, E. UND A. WITTMASTER. "*Gewalztes Gusseisen.*" Essen, 1949: Girardet. (Price DM. 9.30.)

POURBAIX, M. J. N. "*Thermodynamics of Dilute Aqueous Solutions. With Applications to Electrochemistry and Corrosion.*" Translated by J. N. Agar. 8vo, pp. xv + 136. Illustrated. London, 1949: Edward Arnold and Co. (Price 30*s.*)

REED, F. R. C. "*The Geology of the British Empire.*" 2nd Edition, 8vo, pp. ix + 764. Illustrated. London, 1949: Edward Arnold and Co. Ltd. (Price 70*s.*)

STEFFES, M. "*Das Eisen Kohlenstoff Diagramm.*" Pp. 39. (Gloors' Kleine Werkbibliothek). Basle, 1949: Verlag für Wissenschaft, Technik und Industrie.

SYNDICAT GÉNÉRAL DES FONDEURS DE FRANCE ET INDUSTRIES CONNEXES. "*Annuaire* 1949." La. 8vo, pp. 400. Paris, 1949. (Price 350 frs.)